SEPTEMBER 1955

THE WELDING JOURNAL

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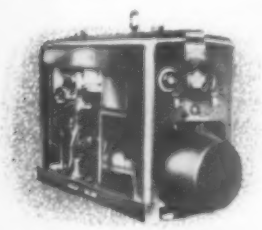

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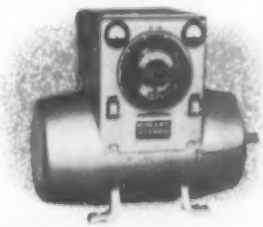

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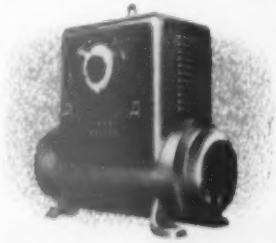

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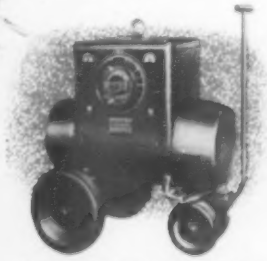

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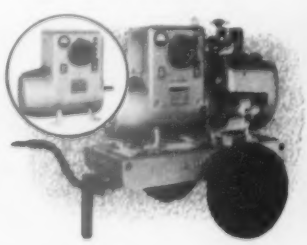

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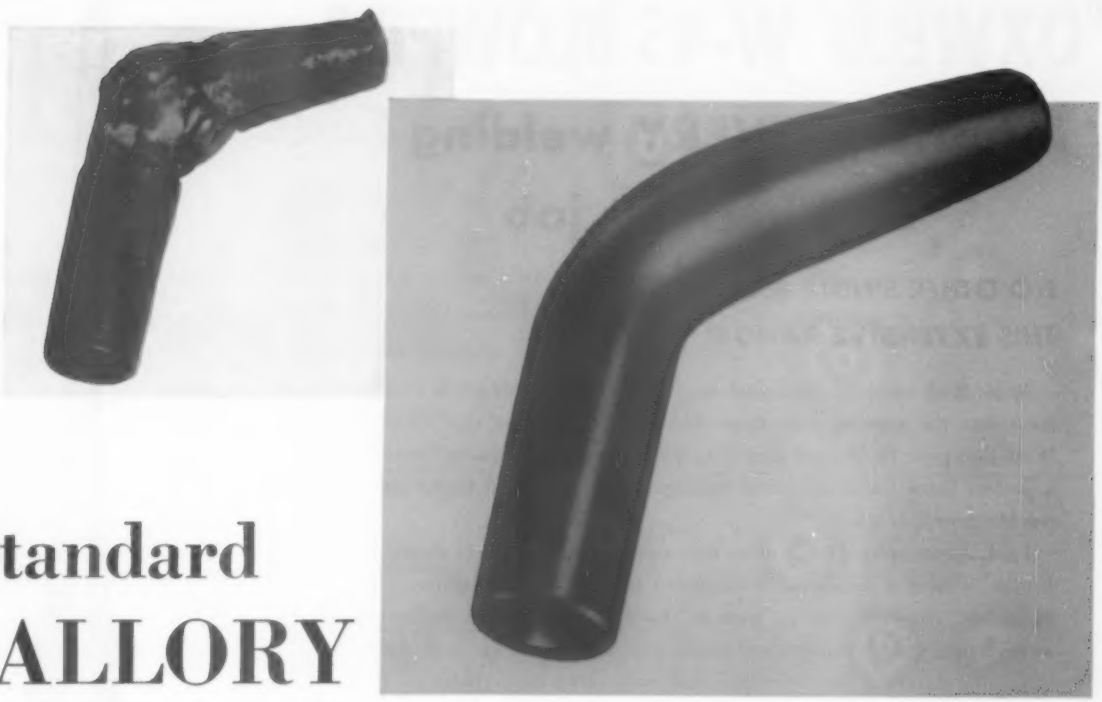

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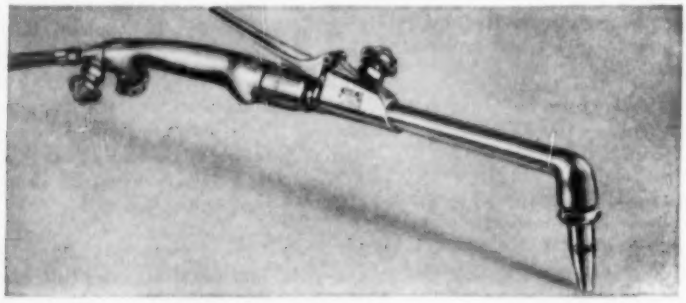

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EDITORIAL

OUR TECHNICAL MANPOWER SUPPLY

The Welding Industry, like most others, is vitally dependent upon an adequate supply of technical manpower. We do not have this at present, so we must recognize the unpleasant fact and the disturbing trend.

Our technical manpower supply is far short of demand. We should all be aware of this pressing problem and strive for its solution.

Deficiencies in our secondary schooling are among the most potent factors in this unsatisfactory situation. The supply of high school science teachers is dwindling seriously. In *The Science Teacher* for September 1954, Ray C. Maul, of the National Education Association, said that, "It is inescapable that in 1954—or in 1955 at the latest—there will not be enough college graduates prepared for high school teaching to meet the demand even if every one of them should seek a teaching position." This trend has been evident for years. The reasons are mainly economic. We do not pay our teachers well enough. Those of a scientific and technical bent do better in industry. Each of us can and should raise his voice in his community for improved economic status of teachers, and especially science and mathematics teachers.

A very bad result of the dwindling supply is that science and mathematics, in many schools, are not necessarily taught by teachers who have specialized in these fields.

Moreover, there has been, in recent years, an alarming drop in the percentage of high school student enrollment in science and mathematics. Another very unsatisfactory situation is the complete omission of scientific courses from the curricula of many high schools. Easy electives are the order of the day, a dangerous development for the industrial age in which we now live. To quote from an article in the *New York Times* for June 19, 1955:

"Half of the nation's high schools do not offer courses in chemistry. Fifty-three percent do not offer courses in physics.

"Since 1900 the percentage of high school students studying algebra has declined from 56 percent to 24.6 percent; those studying geometry from 27.4 percent to 11.6 percent and those studying physics from 19 percent to 4.5 percent."

Alarming, isn't it? Let us do something about it, before industry becomes dangerously starved for lack of adequately trained personnel. The problem is ours—the Welding Industry and the Welding Profession.

O. B. J. Fraser
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Spot-welding operation in the manufacture of metal doors

CONSUMABLE-ELECTRODE INERT-ARC SPOT WELDING

BY R. L. HACKMAN

Detailed description of process, its advantages and limitations, field of applications, and range of usefulness in terms of material types and thicknesses

R. L. Hackman is Development Engineer, Linde Air Products Co., Newark, N. J.

Presented at 1955 AWS National Spring Meeting held in Kansas City, Mo., June 7–10, 1955.

The Process

Spot welding with an inert-gas-shielded arc is not a new concept. Spot welding, which utilizes a nonconsumable tungsten electrode with inert-gas shielding, has been in commercial use for about five years. Within its range, this process has proved an extremely valuable production tool. However, it does have limitations. For one thing, it is limited as to the thickness of materials which can be joined. Another limitation is that it can be applied only to clean surfaces. Furthermore, this process adds no filler metal. Consequently, gaps or poor fit-up results in sinks or cavities.

Consumable-electrode inert-arc spot welding has been developed in order to overcome these shortcom-

ings. Basically, the mechanics of this process are the same as those of the consumable-electrode inert-arc welding process, itself. In both, an arc shielded with inert gas is established between the weldment and a consumable filler wire. Here, however, most of the similarity ends. For in the spot-welding process, the ignition and extinction of the arc must be more precisely controlled. Usually the arc current density and voltages used are higher than those in the conventional process. The weld puddle develops and solidifies rapidly. Intense local heat develops instantly; but as the arc exists only briefly, the total heat input is rapidly quenched by the surrounding metal.

The consumable-electrode spot-welding process is equally adaptable to plug and tack welding. The latter two methods merely constitute variations of the basic process.

Equipment and Operation

Equipment Setup

The success of consumable-electrode inert-arc spot welding depends on using equipment which has the following characteristics: instantaneous and positive arc ignition, an unvarying rate of current decay at the end of each weld cycle and a constant rate of filler wire feed into the arc. Equipment incorporating these characteristics is shown in Fig. 2. The entire setup consists of four units:

1. Automatic controls.
2. Wire-drive unit with conventional wire feed.
3. Manual torch where the shielding gas and wire electrode are fed through the nozzle.
4. Constant potential power supply.

Referring to Fig. 2, the automatic controls are mounted in the opened box shown in the upper portion of the illustration. Directly underneath is the wire-drive unit, which can be wheeled from station to station. In this illustration, the torch is lying on top of the wire drive unit. Water and the shielding gas are supplied through hoses which run to the wire-drive unit, and thence to the torch.

Weld cycles in consumable-electrode spot welding are very short. For this reason, it is extremely important that arc ignition be instantaneous and positive. This enables arc stability to be achieved rapidly. Small variations in the time required to obtain stability may represent a sizable proportion of total arc time, particularly in thin-gage metal applications. Thus, it can be seen that the performance of any consumable-electrode spot-welding equipment can be most easily evaluated when joining thin-gage metals.

A second requirement is an unvarying rate of current decay at the end of each weld cycle. It is this current-decay characteristic which eliminates the possibility of cratering and weld cracking. Since the current of a constant-potential power source is dependent on the wire-feed speed, the rate with which the current decays at the end of a cycle can be determined by the rate with which the rod is decelerated. A further purpose is served in preventing the filler wire from sticking in the weld puddle since this decay characteristic is obtained by stopping the wire feed and permitting the wire to burn back a predetermined amount before the welding power is interrupted. Instantaneous arc ignition and controlled current decay can easily be obtained with the use of constant-potential power sources. In addition to the requirements of instantaneous arc ignition and controlled current decay, it is equally important that the same amount of filler wire be delivered to the arc in the same amount of time. This means a fixed wire acceleration to a given level.

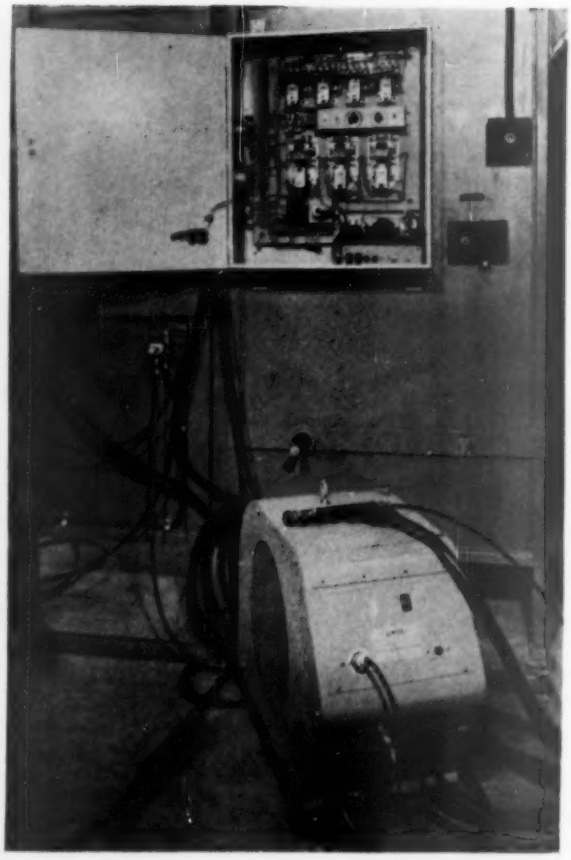


Fig. 2 Equipment for consumable-electrode inert-arc spot welding

Operation

Little welding skill is required on the part of the operator. He simply places the nozzle of the consumable-electrode welding torch against the metals to be joined and squeezes the trigger. No forging pressure is needed. It is necessary only to position the torch squarely. Once the trigger has been pulled, the consumable-electrode welding control takes over and the operator merely keeps the torch in position during the welding cycle. Therefore, if the operator serves only as the operator and not as the setup man, a few minutes of training will completely familiarize him with the equipment.

For setting up the control for a particular operation,

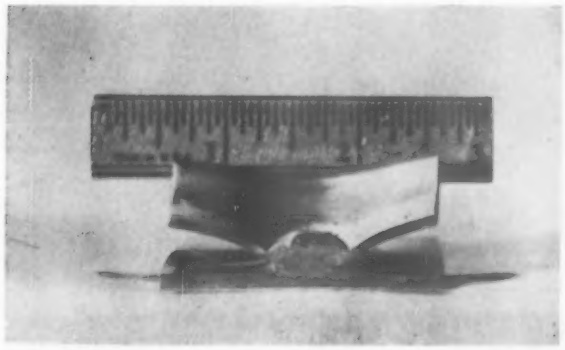


Fig. 3 Cross-section of consumable-electrode inert-arc spot weld after pulling in shear to yield point

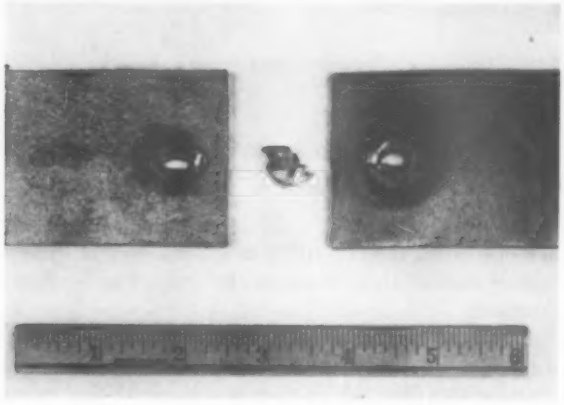


Fig. 4 Spot-weld nugget torn from base metal

a knowledge of normal consumable-electrode welding is beneficial. The general requirements for normal consumable-electrode welding also apply to the spot welding process. Current must not be high enough to cause blasting, but on the other hand it must not be so low as to cause unsatisfactory metal transfer. If the voltage is too high, the arc will tend to cut through the weldment with a very deep, narrow penetration. This will result in small interface nuggets.

Preparation of the material will depend on the strength required in the finished work. In other words, some rust can be tolerated on the interfaces if extremely high strength is not required. However, in order to achieve the ultimate with the process, clean interfaces are necessary.

Process Weld Characteristics

Figures 3 and 4 indicate what can be expected of spot welds made with the consumable-electrode inert-arc process. Figure 3 shows the cross section of a typical spot weld, which was pulled in shear to its yield point. Similarly, Fig. 4 shows a nugget which was torn out of both the upper and lower plates without shearing.

Referring to Fig. 3, 15-gage type 304 stainless steel was spotwelded to 11-gage type 304 stainless steel. When pulled in shear, the weld yielded at 8300 lb, a value well in excess of the 2950 lb minimum* which is recommended practice. (The top sheet was pulled toward and the bottom sheet was pulled away from the reader.) Examination after pulling and sectioning disclosed a wide area of shallow penetration. Examination also showed that shearing was starting to occur in two planes—horizontally through the button and vertically around the button. Cratering was absent, and there was no noticeable evidence of splatter.

Figure 4 is particularly interesting. It depicts what happened as the result of a tensile test on $^1/_8$- to $^1/_4$-in. carbon steel joined by means of the process. As previously indicated, the nugget tore completely out of both the upper and lower plates without shearing. In this particular instance, the sample yielded at 9830 lb.

Applications of the Process

Present Limitations

At present, the consumable-electrode inert-arc spot-welding process can be used on carbon steel, stainless steel and copper-bearing alloys. Aluminum is presently omitted from this list due to some difficulty in obtaining consistency in starting. As pointed out previously, it is extremely important that starting consistency be obtained if uniformly good results are to be expected. The consumable-electrode spot-welding process can be used on metals up to about $^1/_8$-in. thick in the downhand position. On thicknesses of $^1/_8$ in. or thicker, it is necessary to plug weld.

The dividing line as to when it is advisable to plug weld in preference to spot weld is determined for the most part by the strength requirement and consistency necessary. As thicknesses of $^1/_8$ in. are approached, results now obtained with spot welding tend to be less consistent than results obtained with plug welding. Both spot welding and plug welding can be used in position up to about 45 deg to the vertical. Above this point in most cases, plug welding may be preferred; although the consumable-electrode spot welding can now be used on thinner materials—even in the vertical position. Results which can be obtained in the vertical position are generally almost as good as those that can be obtained with downhand spot welding. However, more care must be taken in the selection of welding conditions and in torch-nozzle design. Overhead spot and plug welding with the consumable-electrode process generally can be done as easily as in the downhand position.

The fitup of the materials being welded does not constitute the problem it does in the tungsten electrode welding process. This is because filler metal added in the consumable-electrode spot-welding process can bridge the gap between the pieces. In fact, in many cases, it has been found that higher shear strength occurs with the consumable-electrode process when a slight gap exists. This is probably due to the ease with which the filler metal can flow over the lower plate and thereby effect a larger interface nugget. When larger interface nuggets cannot be obtained with the consumable-electrode process without burning through the

* Interpolated from 2800 lb for 0.070-in. and 3400 lb for 0.078-in. stainless steels in Table 2, Welding Handbook, Third Edition, p. 404.

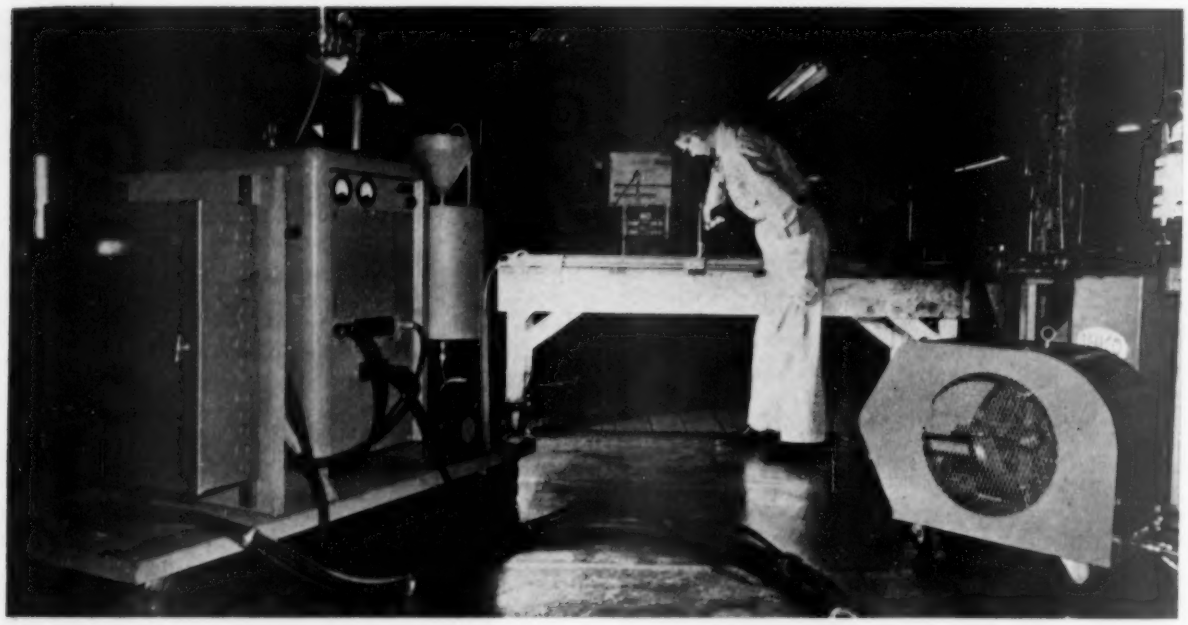


Fig. 5 Consumable-electrode inert-arc spot welding set-up for the manufacture of metal doors

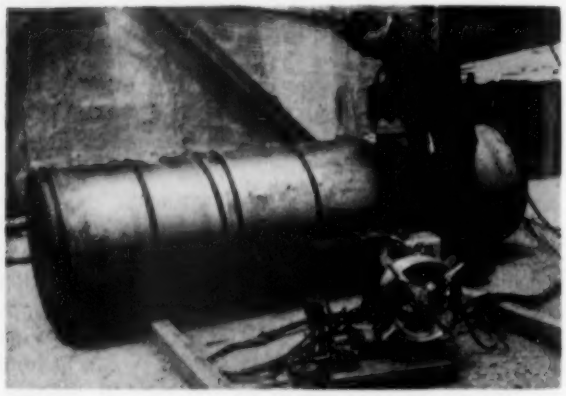


Fig. 6 Consumable-electrode inert-arc spot welding for graphite electrodes

lower plate, multiple cycling has been found useful. In cases where multiple cycling has been used, it has been possible to obtain shear strengths as much as twice those obtained with single-cycle spot weld. Multiple cycling is also beneficial in eliminating hot-short and shrinkage cracking, which may occur in some cases with single cycles. In some cases where the cycle was selected to give large nugget sizes, underbead or crater cracking occurred when joining stainless to carbon steel and stainless to stainless. Multiple cycles in these cases tended to permit the spreading of the heat so that a severe temperature gradient did not develop.

Specific Examples

Consumable-electrode inert-arc spot welding is being adopted more and more for diverse production applications. The examples, which are described and illustrated in this paper, have been selected for purposes of demonstrating the versatility of the process. Explanation of each of these examples follows below by figure number:

Figure 5 shows a complete consumable-electrode spot-welding setup for the manufacture of metal doors. The welding controls, the cooling water circulating pump and tank, along with the argon cylinder and regulator, are mounted on the dolly as shown to the left. The unit at the right contains the wire-feed mechanism, gas and water solenoids, current relay and water-flow switch. The operator is spot welding $^1/_8$-in. thick carbon-steel hinges to a 16-gage cold-rolled door frame, with the weld made through the 16-gage material.

The lead illustration (Fig. 1) is a close-up view of another operation in the production of doors as shown in Fig. 5. Here an operator is positioning the torch prior to spot welding a 16-gage clip to the 16-gage cold-rolled carbon-steel door frame. The welding conditions are 375 amp and 30 v at a welding speed of 1 sec per spot weld.

In Fig. 6, an operator is spot welding a carbon-steel casing on a graphite electrode. This electrode will be used in a furnace for ferro-manganese reduction. Consumable-electrode inert-arc spot welding is the only process known to this user which can be used to fabricate electrode sheathing successfully so as to withstand furnace operation continuously.

Shown in Fig. 7 is the seat framework of a metal chair which was spot welded together by means of the consumable-electrode process. The joint geometry of each spot weld is actually that of a fillet tack. There are six spot welds per chair.

Figure 8 illustrates the production of the chair shown in Fig. 7. Using the jig assembly shown here, chairs are being welded together at a rate of 5 per min. In-

cluding torch positioning and positioning in the jig, this means that a consumable-electrode spot weld is being made once every 2 sec.

Shown in Fig. 9 is a dimpled cold wall for a refrigerated milk tank where the refrigerant will be Freon gas at 150 psi gage. The round portions are spot welds which were made with the consumable-electrode process. Compared to plug welding, the process was three times faster and used one-half the amount of filler wire previously required.

Figure 10 is the back of the cold wall shown in Fig. 9, where 15-gage dimpled sheet was spot welded to 11-gage flat sheet. (Both sheets were Type 304 stainless steel.) The evenness of dark spots indicates the evenness of heat distribution. Welding conditions for this application were 220 amp, 28 v with welding cycles of 0.55 sec and off-cycles of 1.9 sec with a post flow of 1.9 sec.

Figure 11 illustrates three distinctly different types of consumable-electrode process spot welds made in the fabrication of a folding chair. Weld *A* shows the spot weld made when joining the backrest to the leg assembly. Weld *B* is a spot weld joining the hinge assembly to the leg assembly. (The backrest and leg materials were 0.020-in. thick cold-rolled carbon steel.) Weld *C* illustrates a tack weld joining one of the cross bars to the leg assembly.

Shown in Fig. 12 are three views of a magnesium anode assembly for preventing corrosion in hot-water heaters. View *A* shows the top side of the anode before applying the consumable-electrode process. Views *B* and *C* are a side cross-section and top view, respectively, after application of the process. Before applying the process, a carbon-steel rod protruded from the magnesium through the hole in View *A*. Spot welding with the process sealed off the hole in View *A* without the appearance of blowholes in the weld metal due to entrapped air from within the anode assembly.

Fig. 7 Spot-welded seat framework for metal chair

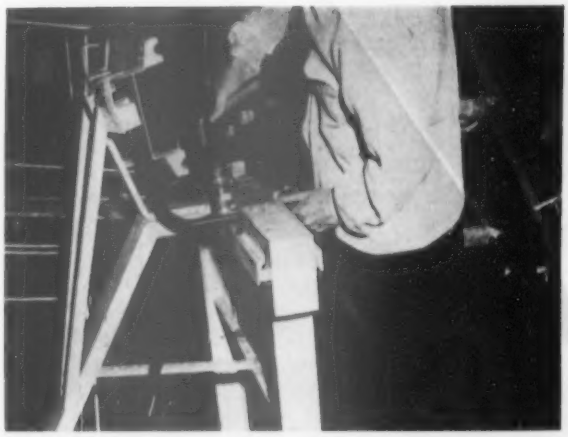


Fig. 8 Method of spot-welding chair seat framework with the consumable-electrode inert-arc process

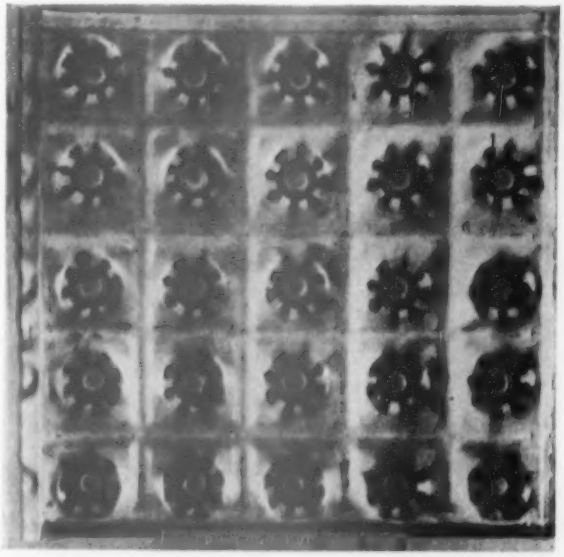


Fig. 9 Consumable-electrode inert-arc spot welds in the cold wall for a refrigerated milk tank

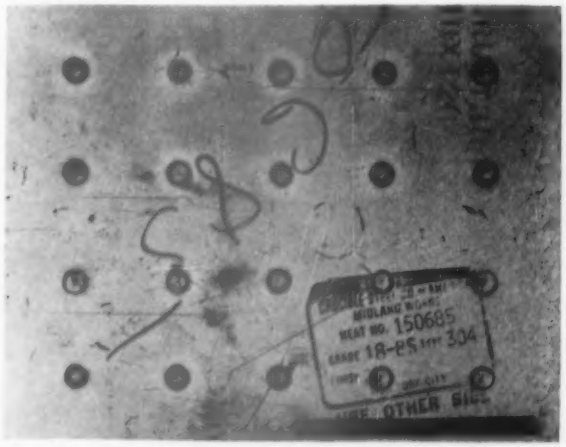


Fig. 10 Back of spot-welded cold wall for a refrigerated milk tank

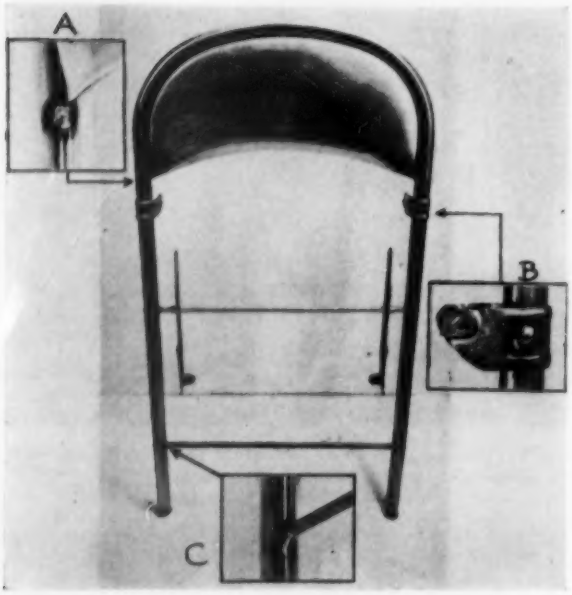


Fig. 11 Consumable-electrode inert-arc spot welds in a folding chair

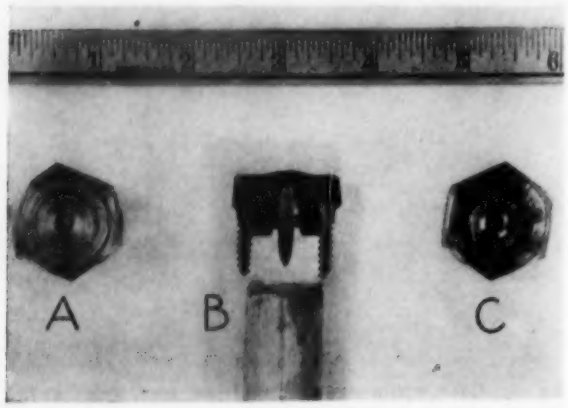


Fig. 12 Application of consumable-electrode spot welding to magnesium anode

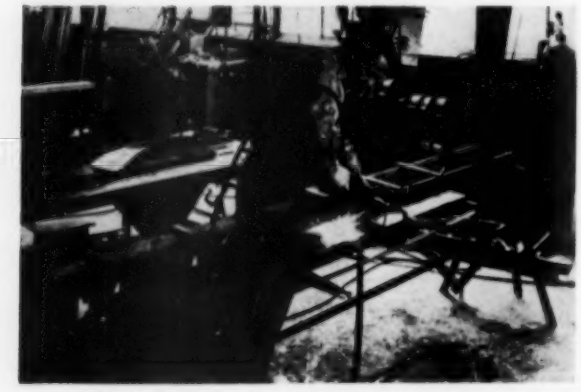


Fig. 13 Application of consumable-electrode inert-arc spot welding to the manufacture of door stiles

The operator shown in Fig. 13 is assembling a door stile by means of consumable-electrode inert-arc spot welding. The process is also used when assembling components of the door itself. Adoption of the process has enabled the customer to increase his production of doors and door frames.

Quality-Control Investigations

Investigations are now under way to determine the statistical reproducibility of spot welds made with the consumable-electrode inert-arc process. Although not yet conclusive, evidence to date has been encouraging. It indicates the following with respect to shear-strength tests:

1. For any given number of samples, shear strength values are symmetrically distributed around the mean shear strength for each group of samples.
2. For a given material, the mean shear strength of samples appears to be a function of material thickness.
3. For a given material, dispersion (as measured by standard deviation) around the mean shear strength of each group of samples appears to be a function of material thickness.

Figure 14 shows how sample shear-strength values for different welded materials vary symmetrically around their respective sample mean values. (For all instances, the actual distribution of sample shear strengths closely approximates theoretical normal distribution.) Similarly, Fig. 15 indicates how sample mean shear strengths appear to be a linear function of material thickness. Finally, Fig. 16 depicts the general relationship, which appears to exist between deviation from sample mean shear-strength values and material thickness.

When studied in relation to each other, all three figures indicate that the process, itself, is amenable to a significant degree of control and statistical prediction in production. Results depicted by the three figures indicate that variations in process performance will be attributable primarily to differences in material thickness. The extent to which the filler wire will affect results is, of course, now subject to question. In this respect, how-

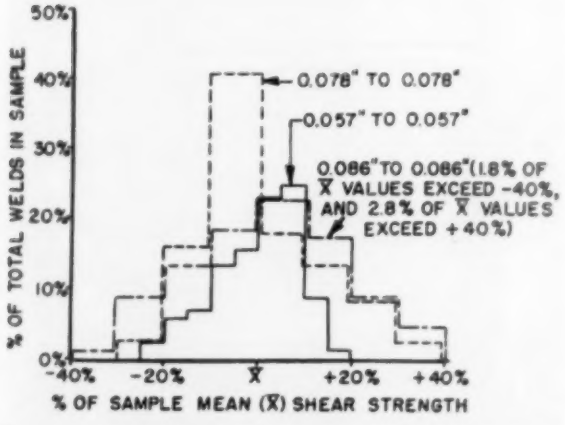


Fig. 14 Frequency distribution of consumable-electrode inert-arc spot welds in cold-rolled low-carbon steel

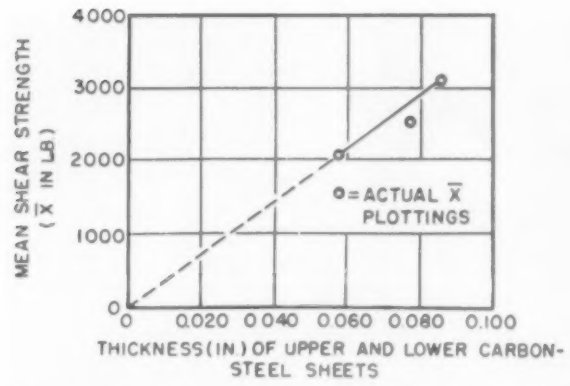


Fig. 15 Mean shear strength of consumable-electrode inert-arc spot welds in cold-rolled low-carbon steel

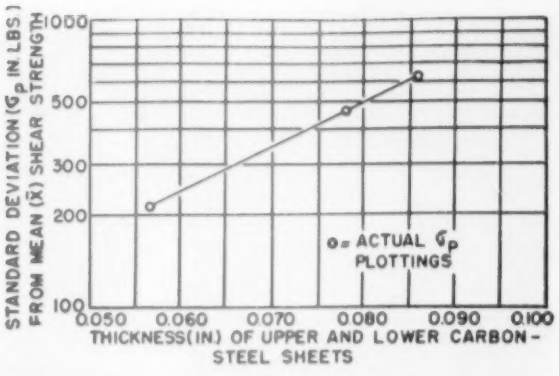


Fig. 16 Shear-strength dispersion of consumable-electrode inert-arc spot welds in cold-rolled low-carbon steel

ever, some sample groups were spot welded with somewhat different types of filler wire. Test results indicated no significant change in sample mean shear strengths or in dispersion around these means. This served as further proof as to the high degree of fusion between the materials, which were spot welded together.

The concepts under discussion here can be used to predict how many consumable-electrode inert-arc spot welds will fall above or below a specified shear strength value. For example, 1640 lb* is recommended as a minimum shear strength when spot welding 0.057- to 0.057-in. low carbon steel. Using a probability table and applying data in Figs. 15 and 16, it is possible to predict that only one in every 145 spot welds will fall below the specified value of 1640 lb.

Comparison to Other Processes

Generally, consumable-electrode inert-arc spot welding must be regarded as a supplement to other existing spot, plug and stud welding methods. The tungsten-electrode spot-welding process is still to be preferred on very thin materials, where it is cheaper and does not have the buildup or button which occurs with the consumable-electrode process as a rule. However, consumable-electrode spot welding does have advantages in that fit-up is not a problem and in that it can be used over a very wide range of thicknesses.

Manual plug welding may still be preferred in some cases where the plug is extremely large or where it is necessary to get a high degree of manipulation of the puddle in order to eliminate cold shuts or slagging. The consumable-electrode process may supplant some applications in which stud welding is now used. For tack welding, consumable-electrode spot welding offers an extremely valuable tool. Regardless of the method which may ultimately be used to weld the pieces being joined, consumable-electrode inert-arc spot welding offers a quick, clean method of tacking. No skill is required on the part of the operator, and it is not necessary to remove any slag before a final weld is placed over the tack.

* Interpolated from 1350 lb for 0.050-in. and 1850 lb for 0.062-in. carbon steels in Table I, Welding Handbook, Third Edition, p. 403.

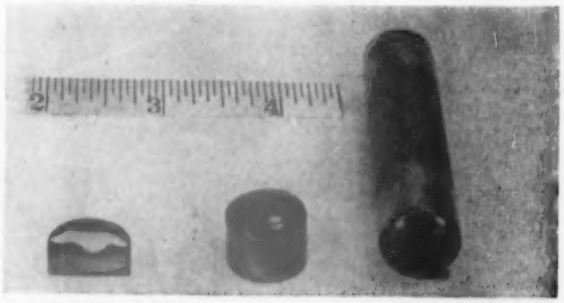


Fig. 17 Hard-faced surface produced with consumable-electrode inert-arc spot welding

Conclusion

Consumable-electrode inert-arc spot welding can be applied easily and quickly to a variety of production problems. The process is now being used in all positions on carbon, stainless and galvanized steels as well as on copper-bearing alloys.

Recent developments indicate that the process has potentials far beyond the present scope of spot, plug or tack welding. Figure 17 illustrates one such potential. It shows three views of carbon-steel bar stock which was hard-surfaced by means of the process. The surfacing metal was deposited with one shot; no preheating was required. However, some dilution occurred. Dilution will be eliminated in the future by using bar stock with concave ends so as to permit two passes with the surfacing material. This application simulates the hardfacing of pusher rods for valve assemblies subject to high temperatures.

The process is now an accepted production tool. Its future growth will follow three major paths: First, it will serve more and more as a useful supplement to older methods of spot, plug and tack welding. Second, its inherent quality-control properties will help to make it an adjunct to automation-production operations. Finally, it will very possibly find use in applications which cannot be adequately handled by any other presently known welding method.

Silver overlaying on a titanium hemisphere used for Gyro assembly by inert-gas tungsten-arc brazing process

METALLURGICAL ASPECTS OF SILVER BRAZING TITANIUM

Paper covers the brazing of titanium by three processes: inert-gas-shielded radiant-heat brazing, induction brazing, and inert-gas tungsten-arc brazing

BY N. A. TINER

N. A. Tiner is associated with North American Aviation, Inc., Downey, Calif.

Presented at the Ninth Western Metal Congress, Los Angeles, Calif., Mar. 28-Apr. 1, 1955.

Titanium can be satisfactorily brazed to itself and to most other metals, but its ability to combine readily with elements of the atmosphere at brazing temperatures practically eliminates all known brazing processes which do not employ inert-gas shielding. Certain fluxes provide some protection, but are not suitable for optimum joint strength efficiency and good corrosion characteristics.

North American Aviation has developed three processes for brazing of titanium; namely, inert-gas-shielded radiant-heat brazing, induction brazing and inert-gas tungsten-arc brazing. Both the radiant-heat and induction brazings are done in a bell jar which contains argon atmosphere. In the radiant-heat process, an incandescent tungsten wire heats the parts to be

brazed. In the induction process, a coil of copper tubing surrounds the work at the joint area and the high-frequency current induced by tubing heats the area to be brazed. Radiant heating requires heating of the whole part and is time consuming, while induction heating requires heating of the joint alone and is not time consuming, but it is not suitable for complex shapes. Inert-gas tungsten-arc brazing is performed by the use of a commercially available water-cooled torch and a high-frequency alternating-current welding transformer.

A brief description of the equipment and the technique used in these brazing processes was given by H. L. Meredith.[1] This paper is concerned principally with the metallurgical aspect of these processes.

Titanium-Silver Phase Diagram

The basis of any systematic study of the metallurgy of brazing is, of course, the constitution diagram which shows the equilibrium temperature and composition limits of all possible alloys formed by the parent metal and the filler material. When titanium comes in contact with molten silver, all of the alloys shown by the diagram at the temperature level of the molten silver will form on the titanium surface. Their order of occurrence, starting from either end of the system, will be the same as in the diagram, assuming, of course, that the diagram is known and that the existing conditions are comparable to equilibrium conditions.

At the time these investigations were made, no published silver-titanium constitution diagram was available. A tentative diagram was speculated by many investigators[2] as in Fig. 1.

Pure titanium has a crystal structure of body center cube (beta titanium) above 1620° F and close-packed hexagonal (alpha titanium) below this temperature. Silver is soluble in beta titanium to a larger extent than alpha titanium. Van Thyne, Rostoker and Kessler[3] published observations on an intermetallic phase, formed by a peritectic reaction between beta titanium and silver-rich melt at about 1900° F. and established its composition as TiAg with a face-centered tetragonal structure. According to a more recent work by Worner,[4] this compound is assumed to be Ti_2Ag.

Raub's work[5] indicated that titanium and silver are immiscible in the liquid state, but no evidence of this was found by other investigators and a partial miscibility was assumed. An attempt was made by the author to determine the approximate solubility limit using the following technique.

Silver was melted in a graphite crucible under argon atmosphere and titanium powder was stirred into the melt. The temperature of the melt was first brought to 2300–2500° F for $^1/_2$ hr and then lowered to about 1800° F and kept there for 2–3 hr. Titanium, being very much lighter than silver, has a tendency to float on the surface of the melt. Dip samples were taken from the bottom portion of the melt by means of a special graphite ladle and analyzed by spectrographic methods. It was observed that the samples taken at 2300–2500° F had 2.7% titanium and those taken at 1800° F had 1.5–1.3%. Solidified samples showed dispersed small round particles of titanium under the microscope (Fig. 1). The amount of titanium retained in molten silver at the end of 2 and 3 hr being close to each other, equilibrium condition appears to be reached, and it can be assumed that the solubility of titanium in molten silver at 1800° F is about 1.5%. It increases at higher temperatures.

From brazing standpoint, the relatively low solubility of titanium in molten silver at temperatures near the melting point of silver indicates that silver should exhibit good spreading power without excessive erosion and undercutting of the titanium base, and permit the use of small clearances in brazed joints. At higher brazing temperatures, erosion and undercutting should increase.

Microstructure of Brazed Joints

On the basis of these theoretical considerations, it can be inferred that in no instance would a silver brazed joint simply be a layer of filler metal mechanically sticking to the surface of titanium. We should observe diffusion of silver into titanium and alloying of titanium with silver to form an intermetallic compound. The amount of the various zones will depend on the conditions of brazing. Some typical microstructures observed are shown in Figs. 2–4.

Figure 2 shows the microstructure produced by silver brazing of a titanium double-lap joint by inert-gas-shielded induction heating. At the silver-titanium interface, a distinct layer is seen. This layer, which is formed by diffusion of silver into titanium and may be

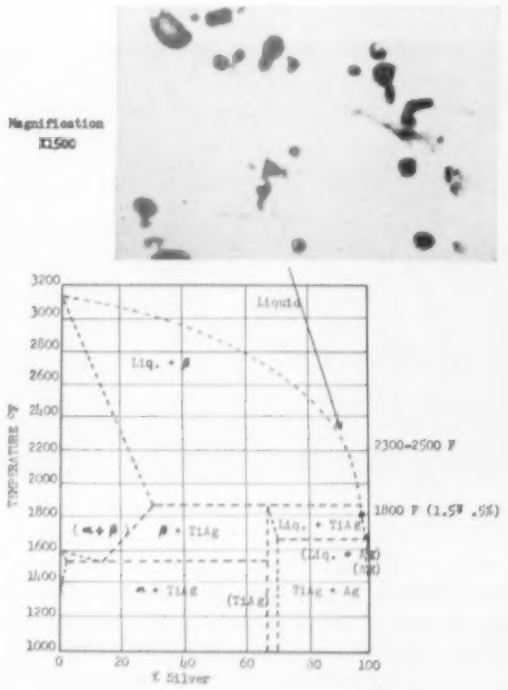


Fig. 1 Tentative titanium-silver phase diagram

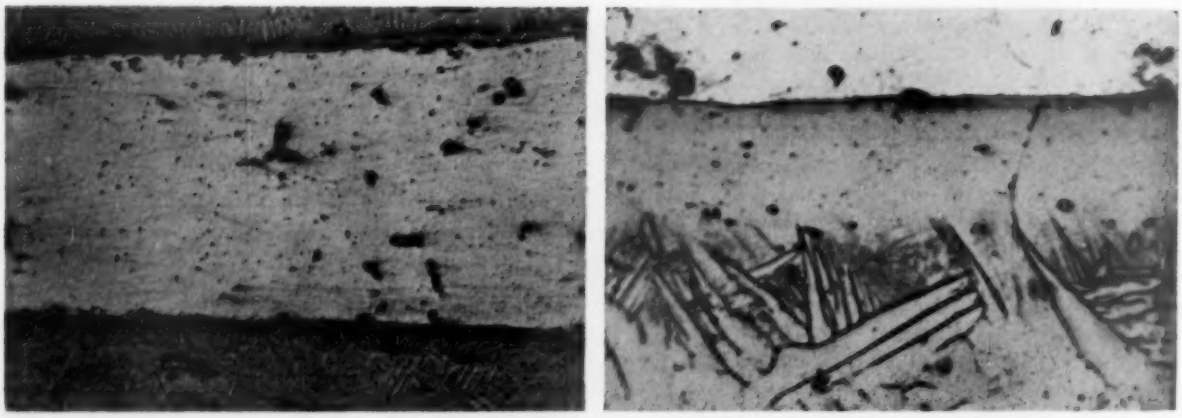


Fig. 2 Silver brazing of a titanium double-lap joint by inert-gas-shielded induction heating

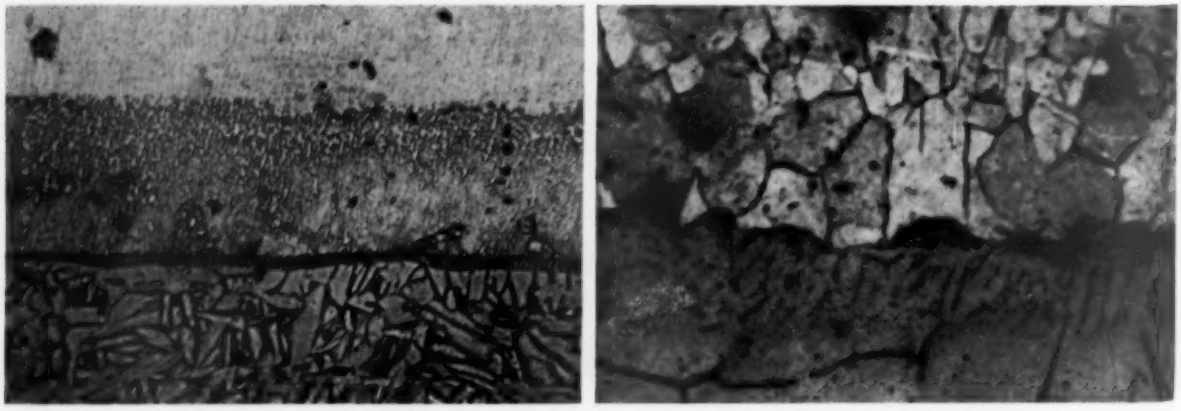


Fig. 3 Silver brazing of a titanium hemisphere by inert-gas-shielded radiant heating

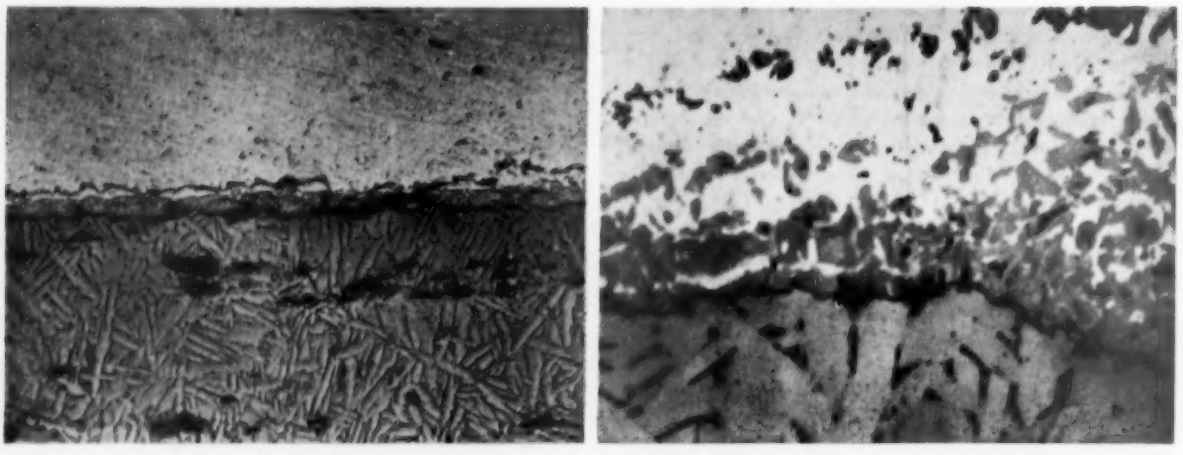
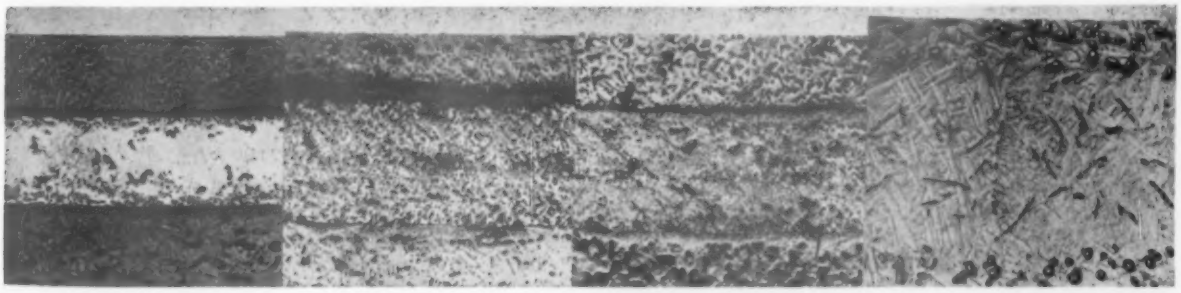


Fig. 4 Silver overlay on a titanium hemisphere by inert-gas tungsten-arc brazing

called "diffusion layer," is a solid solution of silver in alpha titanium. It has an anisotropic structure under polarized light and a Knoop hardness comparable to that of the adjacent titanium. The interstice consists principally of silver phase.

Figure 3 shows the microstructure produced by silver brazing of a titanium hemisphere by inert-gas-shielded radiant heating. The diffusion layer is not homogeneous. Longer heating of the sample appears to cause excessive diffusion of silver into titanium and subsequent

Fig. 5 Microstructure of various shaft insert lap type of silver brazed titanium joints prepared by inert-gas-shielded induction heating; × 250



(a) Brazing time 1 min (b) Brazing time 5 min (c) Brazing time 10 min, high temperature (1900–2000 F) (d) Brazing time 15 min, very high temperature (over 2100 F)

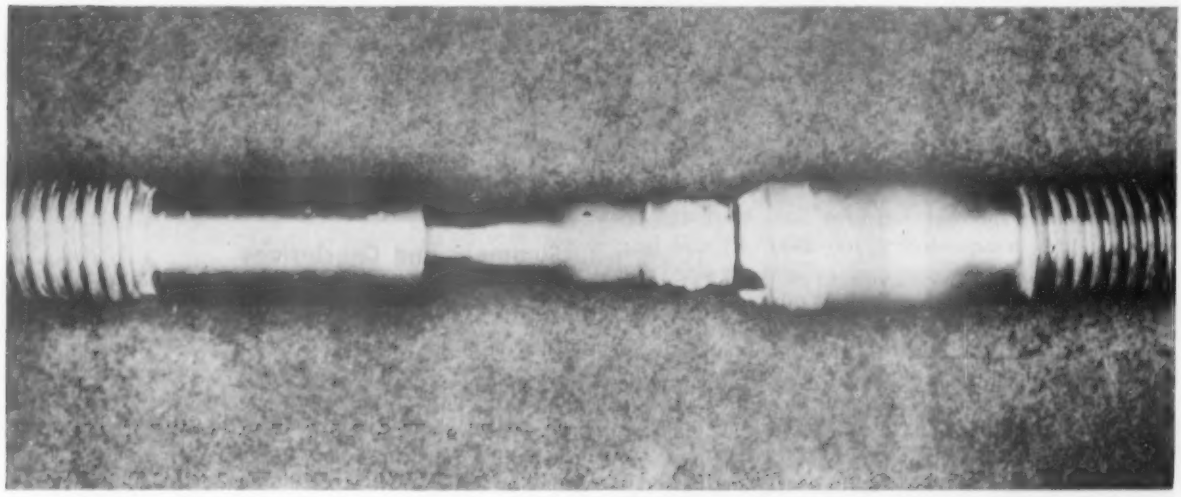


Fig. 6 Fractured tensile shear specimen of silver brazed titanium prepared by inert-gas-shielded induction heating, using very high temperature and long brazing cycle

decomposition of the solid solution. Adjacent to this layer at the silver interface, a very thick zone of columnar grains is seen. It etches more readily than titanium, has a gold-yellow color and a Knoop hardness (200–250 with 25 g load) slightly lower than that of the adjacent titanium (260–380). This alloy zone appears to be the ductile TiAg intermetallic compound identified by Kessler, *et al.* The white area adjacent to the alloy zone is the silver phase.

Figure 4 shows the microstructure of a silver overlay on titanium by inert-gas tungsten-arc brazing. The short heating cycle, relatively high surface temperature, and strong agitation of the molten silver by high-frequency alternating current characteristic to this process produce an alloy zone of small dispersed particles and practically no distinct diffusion layer.

In order to examine more closely the formation of different phases in silver brazing of titanium, four shaft insert lap type of brazed joints were prepared by inert-gas-shielded induction heating, using increasing time and temperature. Typical microstructure of the joints obtained is shown in Fig. 5.

Short-cycle brazed joint (Fig. 5*a*) had a few alloy particles dispersed in silver and practically no distinct diffusion layer. When brazing time was increased, the diffusion layer became distinct and the alloy formation considerably increased (Fig. 5*b*). At higher brazing temperatures, excessive erosion and undercutting occurred on the titanium interface, and light gray particles of retained beta titanium are formed in the alloy zone (Fig. 5*c*). When extremely high brazing temperature and long brazing cycle was used (Fig. 5*d*), both diffusion layer and the alloy zone became practically one continuous layer, nearly twice the thickness of the original interstice. This interstice had a hardness comparable to the adjacent titanium and appeared to be a solid solution of silver in titanium. X-ray diffraction analysis of the powder samples taken from this zone exhibited alpha titanium and TiC lines, and no silver or silver compound lines were found.

Strength of the Brazed Joints

Joint shear strengths associated with these various microstructures have been determined, using tension shear specimens shown in Fig. 6.

In general, the strength of a joint is determined by design factors, such as the type of joint, the joint area and the clearance between the members of the joint. The strength of the filler metal also has an influence. In our investigations, a shaft insert type of lap joint was chosen as it was easily and uniformly heated with a simple work coil in the induction furnace, and the effect of joint clearance was slight. A joint diameter of $^1/_2$ in., a width of $^1/_4$ in. and a clearance of 0.003–0.004 in. on the radius were used.

The tests showed that pure silver brazed joints, made with correct brazing procedures, can develop shear strength of 21,000–24,000 psi. The brazing temperature used was 1800–1900° F and the brazing time was about 1 min, just long enough to allow the filler metal to flow completely through the joint interstice.

If the brazed joints were made by using longer brazing cycles (5 to 10 min) and/or slightly higher brazing temperatures and a thick layer of TiAg compound is allowed to form in the interstice, the joint shear strength was somewhat impaired. If, however, excessively high temperature (2100–2300° F) and long cycle (10–15 min) were used and the joint consisted of a continuous layer of solid solution of silver in titanium, extremely high shear strengths, over 35,000 psi, were developed. This process caused some grain growth and slightly impaired the properties of the titanium base. The joint failed in shear tests partially through the interstice and partially through the parent metal as seen in Fig. 6.

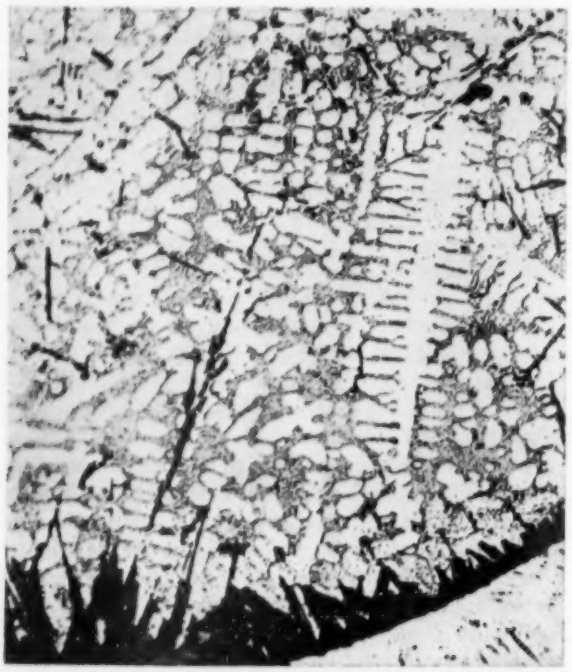


Fig. 7 An overlay of "BT" alloy (72% Ag – 29% Cu) on titanium by inert-gas-shielded induction heating

For comparison a number of brazed joints were made using silver brazing alloys, such as Ag-Cu Ag-Cu-Sn, Ag-Cu-Cd-Zn and Ag-Mn. The shear strength of these alloy joints (16,000–20,000 psi) was somewhat lower than the shear strength of the pure silver joints.

Figure 7 shows the microstructure of a silver-copper overlay on titanium made by inert-gas-shielded induction heating. The interstice consists of an Ag-Cu eutectic and a very brittle intermetallic compound. The presence of such brittle phases in a brazed joint appears to reduce the shear strength of the joint.

The strength of brazed joints at elevated temperatures is generally determined by the filler metal used, and not by the joint design, and the strength becomes weaker as the temperature increases. Ag-Mn alloy is normally used for high temperature applications. The shear strength of the alloy joint drops from about 20,000 psi at room temperature to about 13,000 psi at 800° F. Titanium-brazed joints made dissolving silver in titanium by the use of intense induction heating and long cycle appear to be very promising for such high temperature applications.

Summary and Conclusions

An investigation is made on the metallurgical aspects of silver brazing of titanium by the methods which employ inert-gas shielding.

The silver-titanium constitution diagram is discussed, and the solubility of titanium in molten silver at 1800° F is determined to clarify the silver-rich corner of the constitution diagram.

The microstructure of the various zones produced at the silver-titanium interface by the three inert-gas-shielded brazing methods is illustrated and the relation between the microstructure and the joint strength is pointed out.

A new technique of induction brazing, using high temperature and long heating cycle, is brought to attention. The brazed joints made by this technique had very high shear strength, especially at elevated temperatures, even though the properties of the parent metal adjacent to the joint were somewhat impaired.

Acknowledgments

This paper is based on work done at the North American Aviation, Inc., Downey plant, on silver brazing of titanium. The author wishes to express his appreciation for the permission to publish it, and for the assistance of H. L. Meredith and other members of the Materials Research & Process Development Group in this investigation.

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Fig. 1 KC-97 airplane (transport-tanker)

SPOT WELDING OF STRUCTURAL APPLICATIONS IN AIRFRAME MANUFACTURING

BY W. R. GAIN

High quality and consistency of spot welds assured through understanding of, and careful adherence to, procedures developed over years of investigation and research

Introduction

Spot welding of primary aircraft structures goes back farther than most of us stop to realize. There is, as an example, the amphibious biplane built in 1931 by the Budd Manufacturing Co. This airplane, the "Pioneer," is on display at the Franklin Institute in Philadelphia and demonstrates the utilization of the spot-welding process in fabricating structural applications in corrosion-resistant steels.[1] Jenkins and Piper[2] have referred to the spot-welded wings on the Northrop Gamma-type plane which was in production in 1933. The "Formers" were spot welded to the skin on the Chance Vought "King-fisher" (OS2U-1), a Navy pre-World War II observation plane.

From this we can see that spot welding of aircraft structures is not something new. However, lack of experience and limited knowledge of the various physical and chemical properties of the spot-welded joints necessitated somewhat of a curtailment in the use of spot welding for structural components of aircraft. This was controlled through various means by the applicable Governmental agencies such as the Army Air Corps and the Navy Bureau of Aeronautics.

Through the ensuing years, considerable research and development work was performed. This work provided an accumulation of data which resulted in many design improvements in spot-welding equipment and increased utilization of the process. With the in-

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creased utilization, the field of experience has expanded and brought about additional improvements in equipment dependability, design and operation know-how and a quality level which resulted in relaxing some of the restrictions on the use of resistance welding. With the easing of some restrictions, further steps were made in developing techniques and equipment to provide reliable consistency of joint strengths, proper load distribution and elimination of corrosion problems.

We might then say that spot welding of aluminum alloys in aircraft manufacture was originally a method of assembling boxes, map cases, ash trays and other small parts wherein the failure of one or two welds would not affect the use of the part—even the failure of the entire part would have no serious effect other than that reflecting the inadequacy of the process itself. (In those days, little was known of the causes of weld failures.)

By continual usage and development and research work in spot welding aluminum alloys, the process was gradually improved and the reasons for weld inconsistencies were isolated as functions of the machine operation, the material or the associated cleaning process. These investigations revealed that the cleaning process would produce uniformly clean parts if the solution temperatures, concentrations and the immersion times were

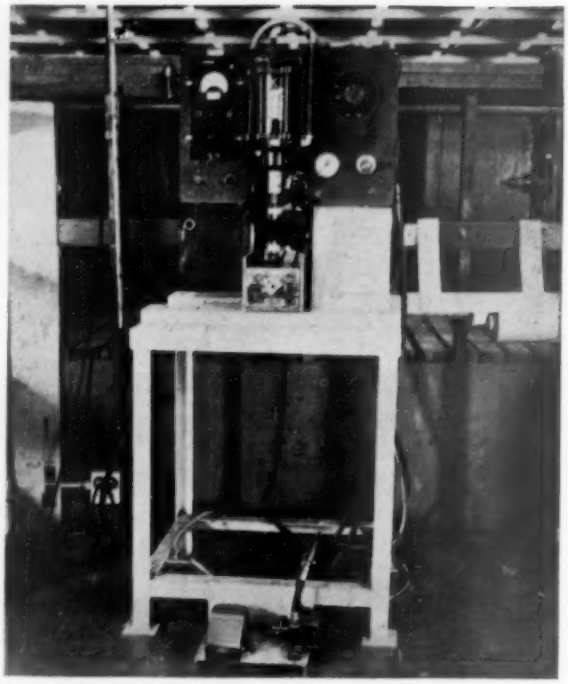
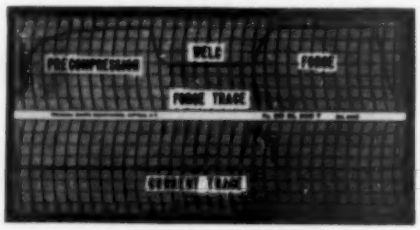


Fig. 2 Surface resistance measuring device (10 microhms min)

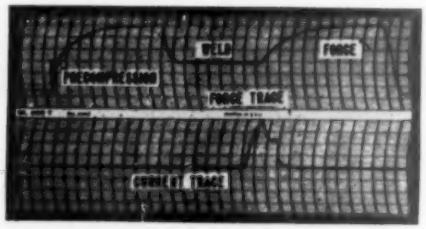
Fig. 3 Oscillographic recordings of welding force and current indicating the rate of rise of the forging force and the time relationship of these two variables to each other

VARIATIONS IN APPLICATION

PISTON TYPE PRESSURE SYSTEM →

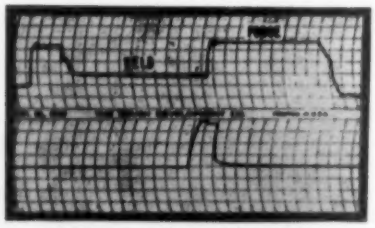


Satisfactory rate of rise of forge force



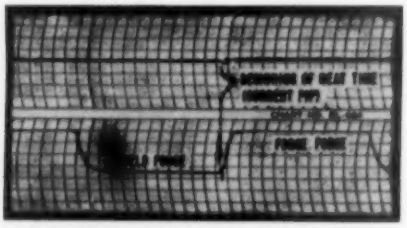
Unsatisfactory rate of rise of forge force

DIAPHRAGM TYPE PRESSURE SYSTEM →

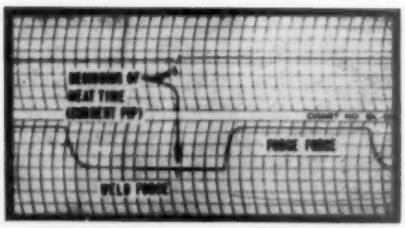


Excellent rate of rise of forge force

FORCE DELAY CONTROL →



Minimum forge (recompression) delay forge initiated $^1/_2$ cycle after beginning of heat time



Maximum forge (recompression) delay forge initiated 10 cycles after beginning of heat time

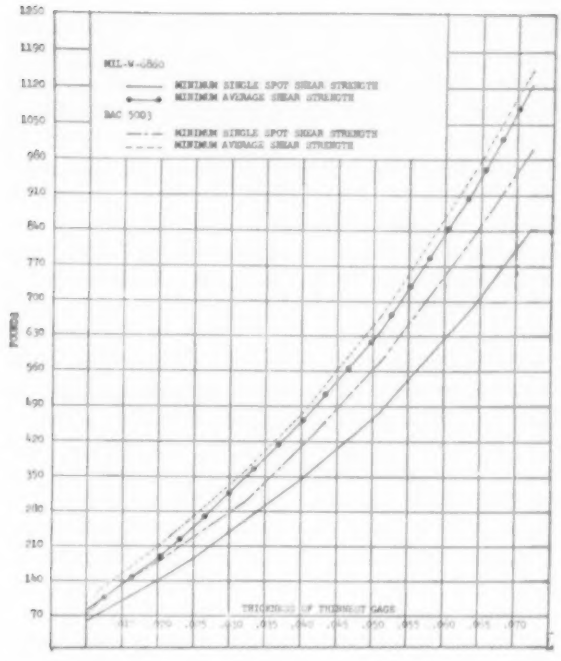


Fig. 4 Minimum single spot and minimum average single spot shear strength requirements

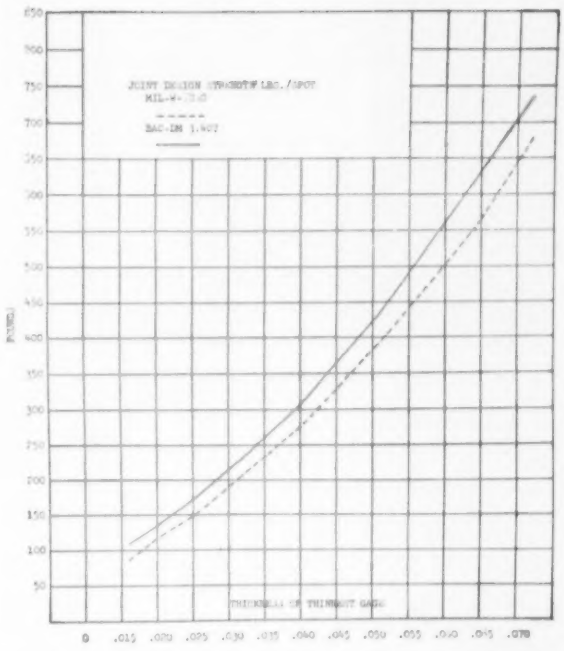


Fig. 5 Single spot design strength based on single spot shear strength requirements, Fig. 4

controlled. A method of checking the consistency and adequacy of the cleaning process was also developed. A typical surface resistance measuring device is shown in Fig. 2.

Concurrently with the above development, investigations of the operations of the equipment revealed the importance of the welding current, welding pressure, forging pressure and electrode contour in producing welds of good quality and consistency. Figure 3 is a photograph of some oscillographic traces that illustrate some of the more recent developments of the welding current wave shape with a sloping wave front and controlled current decay. These traces also show the rapid rate of rise of the forging force and the adjustable range of application with respect to the time relationship of these two variables. The synchronous controls developed to accomplish reproduction of the current wave shapes and the time relationship of the welding current to the welding and forging forces were very important to producing consistently, metallurgically sound welds.

The designer was very anxious to utilize the economical advantages of spot welding such as low cost and high production rate but he could not risk the safety of the airplane by the application of an inconsistent process. By demonstrating the consistency of the process, using carefully controlled procedures, approval was gradually extended to use spot welding in more important structures. So today we find ourselves spot welding, for example, large body panels in which the strength and safety of the airplane depend upon the quality and consistency "built in" the welds by an understanding of, and careful adherence to, the procedures developed over years of investigation and research.

It is sometimes difficult to define specifically what is or is not a structural application. One possible way might be to ask the question "Can failure of this part result in the loss of the airplane and/or the lives of its occupants?" A "yes" answer to this question would mean that the part is primary structure. This discussion will be confined to applications of this type.

The airplane on which these assemblies are spot welded is shown in Fig. 1, and has the following specifications:

Wing span, ft	141
Fuselage length, ft	110
Gross weight, tons	87
Type	Transport-Tanker

Approximately six hundred of these airplanes have been manufactured.

In order to provide for adequate design allowables on the type of applications mentioned above, it was necessary to use minimum single-spot shear strengths and minimum average single-spot shear strengths in excess of those specified in the Military Specification (MIL-W-6860). Figure 4 shows the comparison of the minimum single-spot and minimum average single-spot shear strengths of the two specifications. Figure 5 shows the difference in the allowable design single-spot shear strength.

In addition, the weld quality requirements established for these applications are higher in some cases than those required by Military Specification, as an example. The welds must be crack-free whereas MIL-W-6860 allows cracking within certain limitations.

The most critical spot welding on these applications is

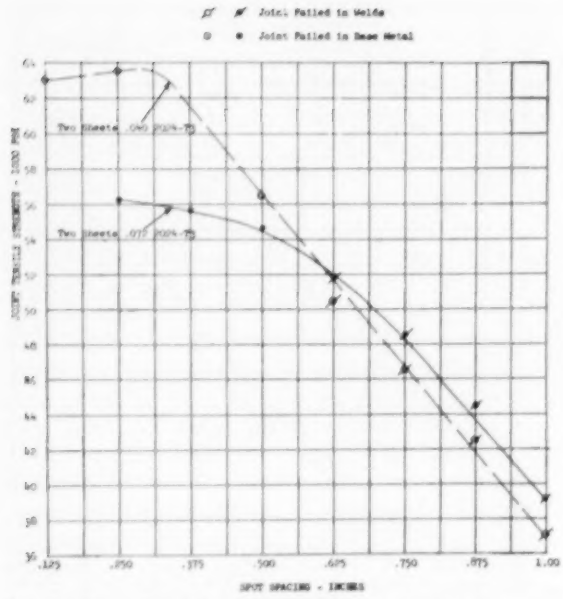


Fig. 6 The effect of spot spacing on ultimate tensile joint strength

in the skin laps on the body panels. When spot welding was contemplated for this type of work, an extensive test program was conducted for the purpose of developing a two-row spot-welded joint[3] which would satisfy the following requirements.

1. Ultimate tensile strength of the lap joints shall exceed 50,000 psi.
2. Joint fracture at ultimate static load shall occur in the base metal and not by shearing the spots.

During the course of this investigation, the optimum values were obtained for: (1) spot spacing, (2) edge margin, (3) row spacing and (4) single-spot shear strength to be used in fabricating the two-row spot-welded joint satisfying the above requirements.

The most favorable values determined for these factors are shown in Table 1. It should be pointed out that the values shown in Table 1 were not necessarily those which gave the maximum joint strength but those which would fulfill the requirements of producing a joint strength of at least 50,000 psi with failure occurring in the base metal and not by shearing the welds. They were selected by weighing the benefits of the strength gained above that required against those of the loss of production time, weight saving and standardization. As an example, Fig. 6 shows the effect of spot spacing on ultimate tensile joint strength. Note that higher joint strengths are obtainable in both the 0.040-in. combination and the 0.072-in. combination with closer spot spacing than 0.5 in. However, in each case the requirements of 50,000 psi joint strength and base metal failure are fulfilled at 0.50-in. spot spacing. In consideration of production time and for standardization of the indexing table racks, the 0.50-in. spot spacing was selected. This type of welding is called special-purpose, high-strength spot welding.

It should be noted that Table 1 only includes sheet thickness combinations through two pieces of 0.072 in. If heavier thicknesses were employed, it would be necessary to determine new values for edge margin row spacing and spot spacing because of the shunting effect and to preclude expulsion of the larger spot. Correspondingly higher, single-spot shear strength values would be required.

Some Structural Applications

A typical body panel is shown in Fig. 7. The resistance welding of the skin lap is the special-purpose type and that joining the stiffeners and patches is Class A (Structural). In some instances the special purpose welding involves a four-thickness pile up. This is in such places where a reinforcement doubler for a cut-out area crosses a lap joint containing a hat section stiffener.

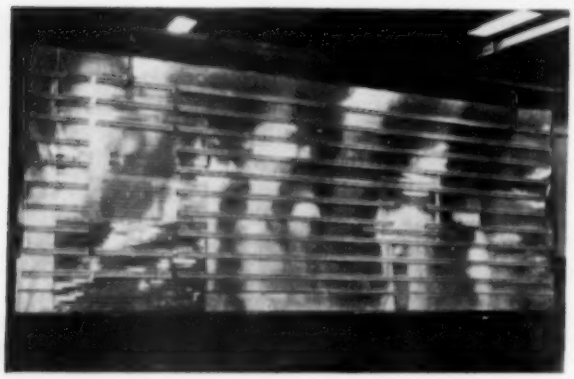
Table 1

Sheet thickness combination, in.	*Spot spacing, in.*	*Edge margin, in.*	*Row spacing, in.*	*Average single-spot shear, lbs.*
0.040 + 0.040	0.5	0.38	1.25	700
0.051 + 0.051	0.5	0.38	1.25	900
0.064 + 0.064	0.5	0.38	1.25	1200
0.072 + 0.072	0.5	0.38	1.25	1400

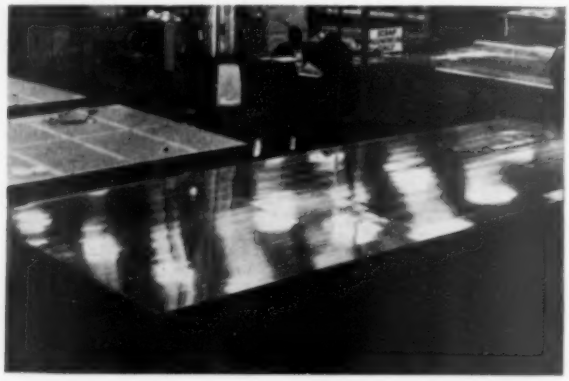
Figure 7

A. View of inside of a typical KC-97 body panel

Note: Panels are flat when welded



B. View of outside of panel A



This condition is shown in Fig. 8, and the combination consists of a 0.040-in. skin, a 0.064-in. skin, a 0.025-in. patch (reinforcement doubler) and a 0.072-in. hat section stiffener. The joint strength under these conditions is based on the skin-to-skin material.

Figure 9 shows a lap joint being roll spot welded. The machine is operated at 40 spots per minute. The patches and stiffeners are similarly roll spot welded except the speed is increased to 70 spots per minute.

There are 21 spot-welded panels in the fuselage. These panels range in size from 10 x 20 ft comprising two 20-ft long skins to 5 x 5 ft, involving only one skin. All of these 21 panels are assembled on two layout tables as shown in Fig. 10. The component parts are drilled approximately every 20 in. for temporary fasteners and code marked so that they can be disassembled for cleaning and reassembled for welding. These two layout tables are adequate for handling the panels for more than 20 airplanes per month. It is quite obvious that tooling requirements are small.

The patches, stiffeners and splice joints are welded in the flat and then assembled to the circumferential stiffeners in wrap-around fixtures as shown in Fig. 11. Figure 12 is a view of a part of the fuselage final assembly area.

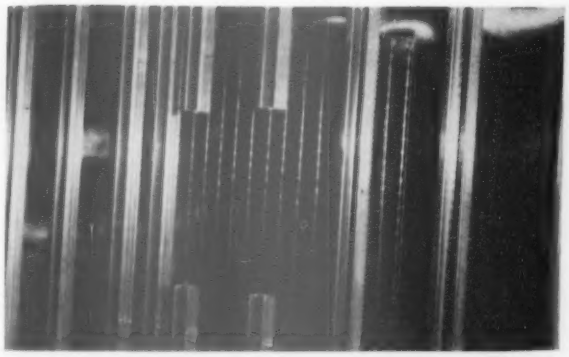


Fig. 8 Close-up showing spot-welded 4-thickness pile-up (skin to skin to doubler to stiffener)

The support fixtures on the spot-welding machines are large flat tables covered with carpeting. The tables for the press-type spot welders are rack indexed and air operated and synchronized with the "off" timer on the spot-welding machine. Small pneumatically operated rollers (see Fig. 13) carry the skins during movement of the tables on both the roll spot and press type spot welders to prevent scraping on the electrodes.

Fig. 9 Roller spot welding a typical KC-97 body panel

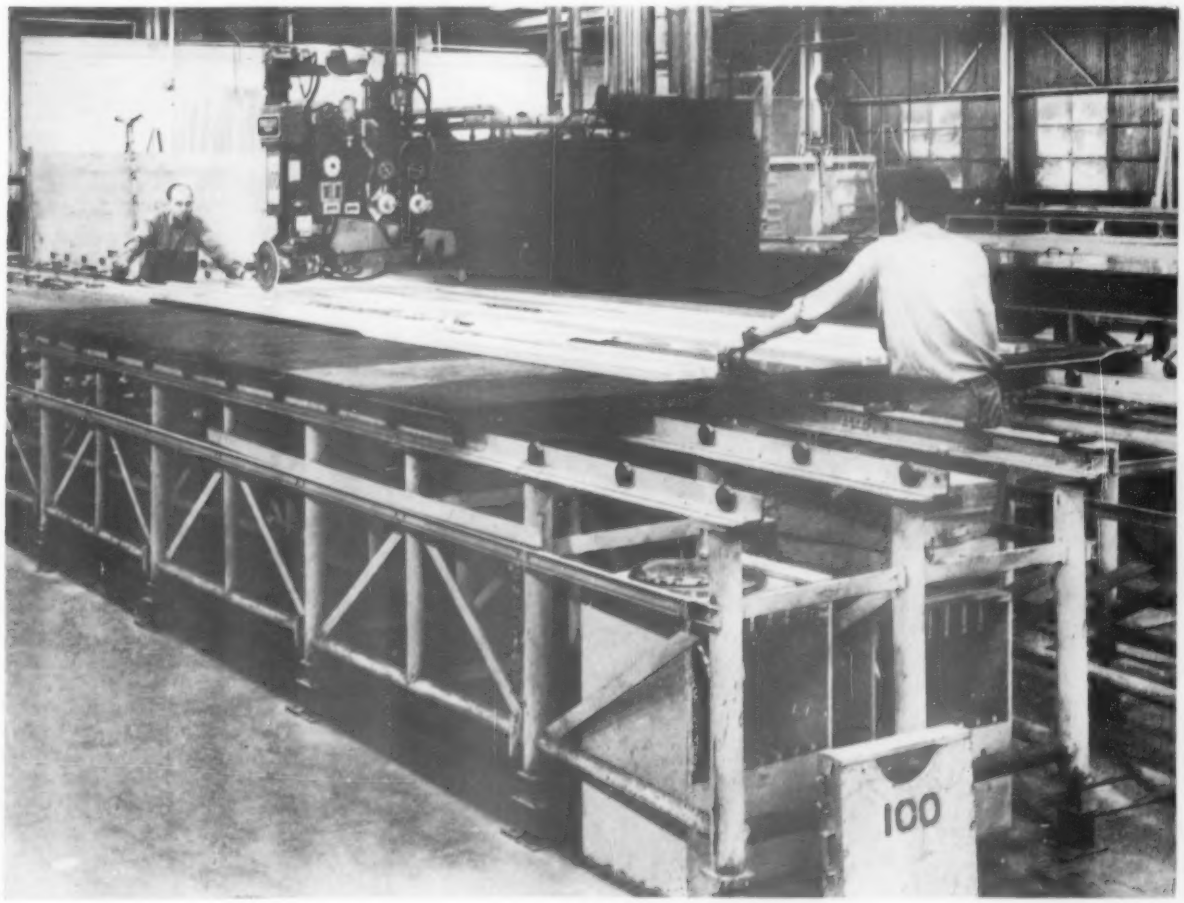


Fig. 10 Components to be spot welded are located on layout table as shown

Fig. 11 After spot welding the panel is joined to circumferential stiffeners in a "wrap around" fixture

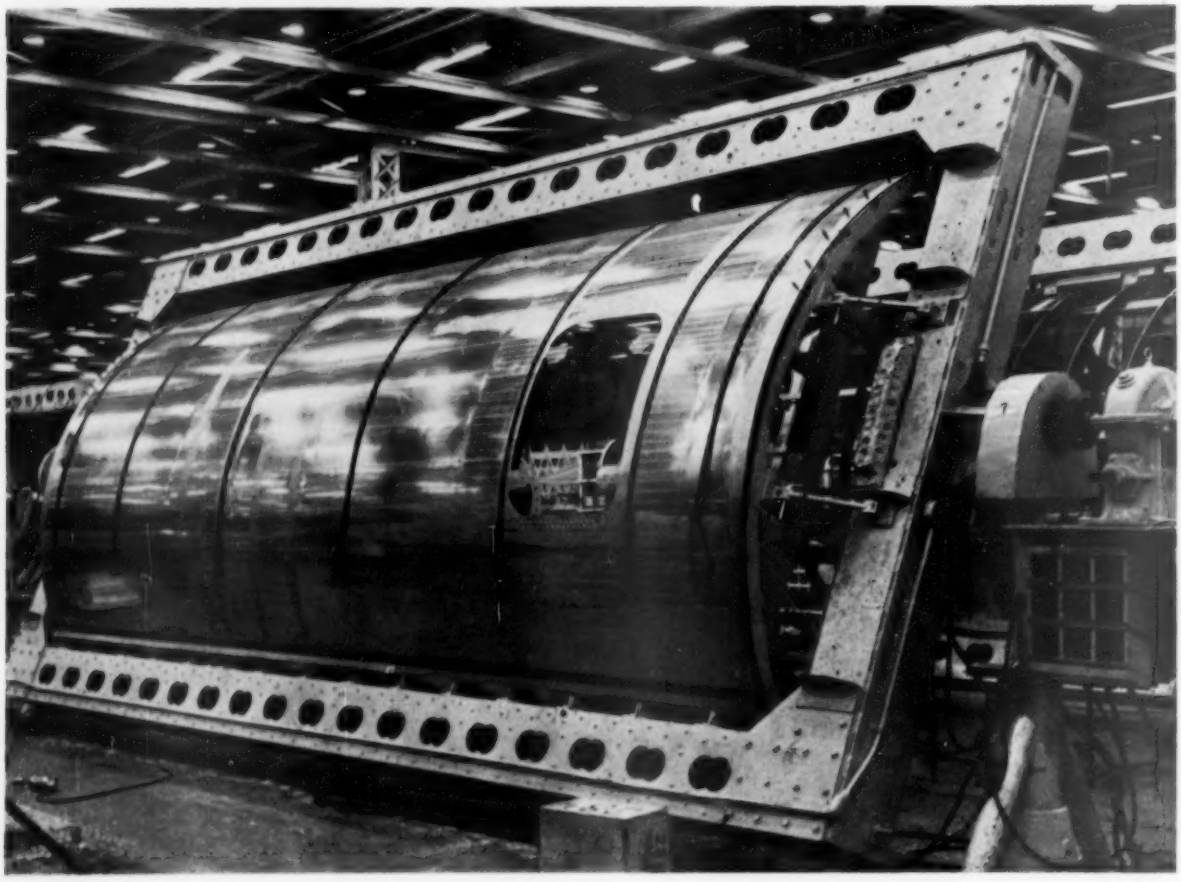


Fig. 12 After attaching to circumferentials, panels are joined to make fuselage sections

The tables on the roll-spot welders are free wheeling and are driven and stopped with the panels by the roll spot-welding machine through the friction of the roll welder wheels. The roll-spot welding is accomplished with intermittent drive and a variable pressure cycle.

Two of the many other structural applications on this airplane are the floor beams and the floor panels. The design is such that considerable loads are transferred from the floor panels through the beams to the body. Class A spot welding is used on these components. The floor beams consist of two channel webs spot welded back to back. A cap strip is then spot welded both top and bottom. All materials are 2024-T3 clad.

The floor panels shown in Fig. 14 consist of a skin and corrugation (2024-T3 clad) joined by roll spot welding. This roll spot welding is accomplished at the rate of 90 to 110 spot welds per minute at 0.62-in. spot spacing. At this rate of welding external flood cooling is used to minimize warpage. Figure 15 shows a floor panel being roll spot welded.

Process Control

It was pointed out earlier that the strength and safety of the airplane depends upon the quality and consistency built into the spot welds by an understanding of, and careful adherence to, the procedures developed over years of investigation and research. This means that the process must be controlled to assure satisfactory results in the end product.

In developing a welding schedule for Class SP (Special Purpose) spot welding it must be demonstrated on each joint combination that the static joint strength

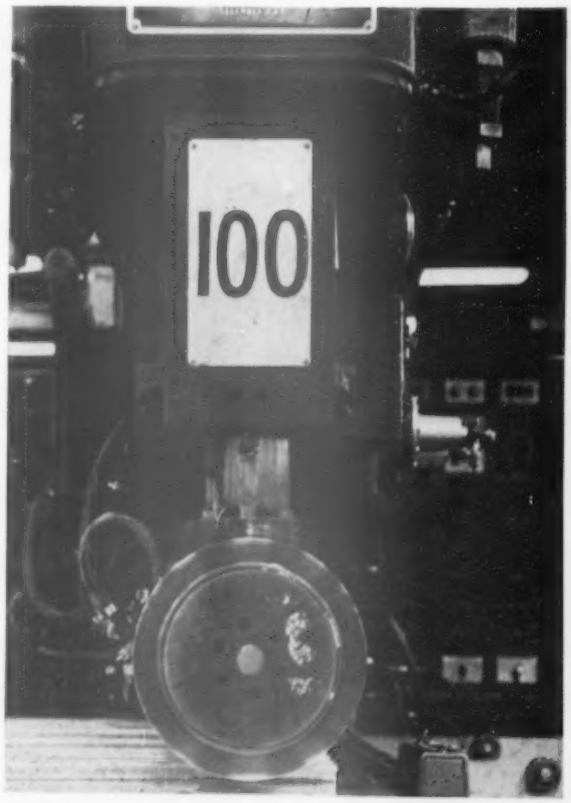


Fig. 13 Pneumatically operated rubber rollers carry skin over lower electrodes during free movement of table to preclude damaging outside surface

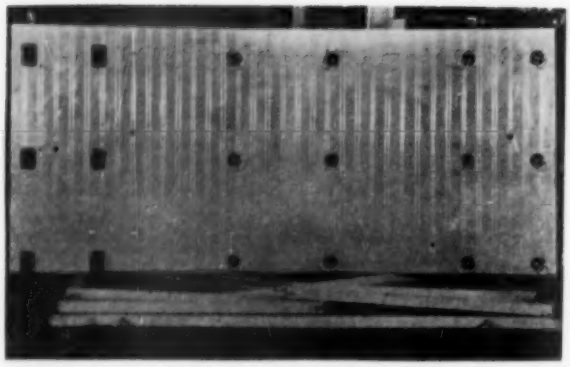


Fig. 14 A typical spot-welded KC-97 floor panel

exceeds 50,000 psi and that the failure occurs in the base metal. This is accomplished by welding and testing a panel as shown in Fig. 16. It is noted that five specimens are tested for ultimate static strength and two specimens are examined metallurgically. The five ultimate tensile strength specimens must average 50,000 psi or greater and no specimen can fail below 47,500 psi. Failure must occur in the base metal in all five specimens.

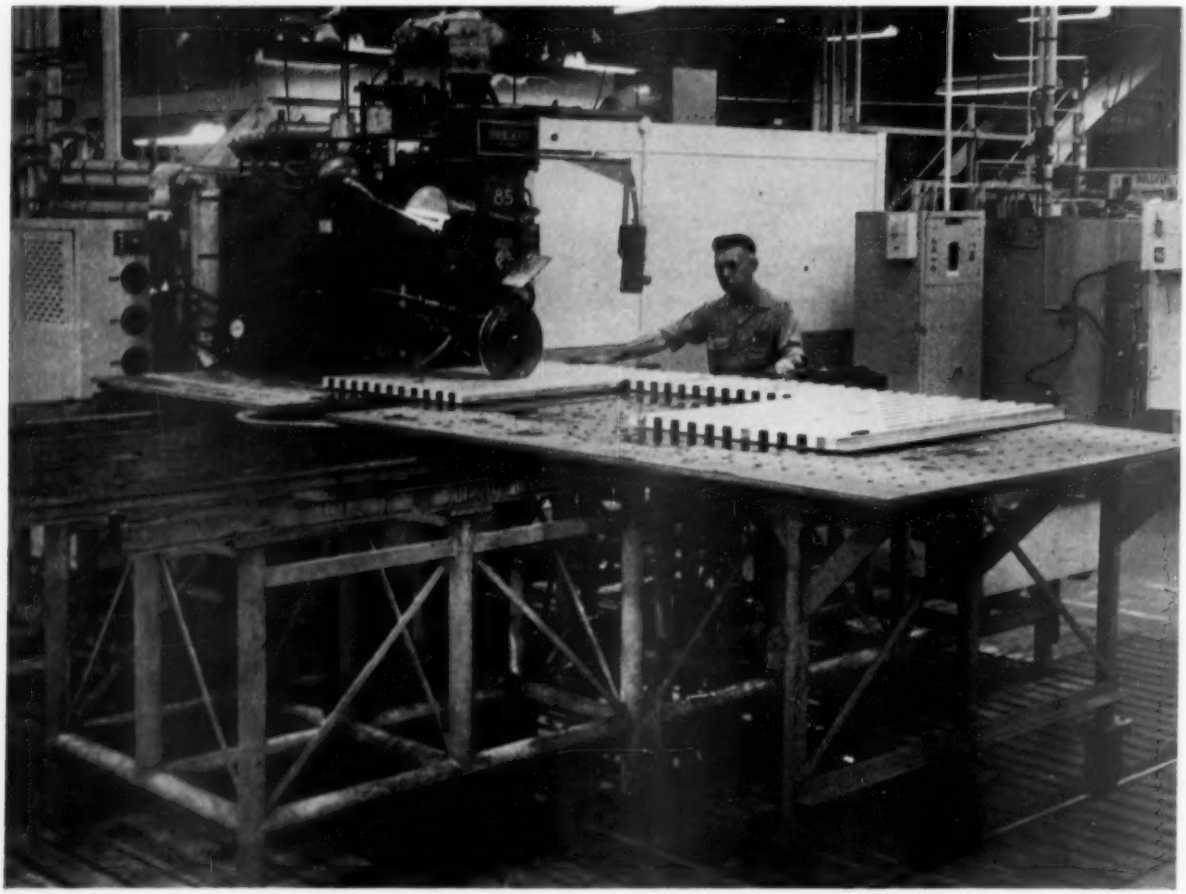
The welds, as indicated by metallurgical examination, must be free of internal defects such as cracks, voids and porosity, and be within the penetration limits of 30 to 80% of the base metal thickness.

A 20-weld, single-spot shear-test panel is welded, cut and tested for each interface to establish the single-spot shear strength average and demonstrate a consistency such that at least 18 of the 20 welds fail within $\pm 12^1/_2\%$ of their average and the remaining two welds must fail within $\pm 25\%$ of the average.

The surface preparation for this class of welding consists of chemical cleaning and deoxidizing resulting in a surface resistance not to exceed 50 microhms and subsequent wire brushing of the faying surfaces. The wire brushing must be accomplished within 8 hr after chemical deoxidizing and the parts assembled within $^1/_2$ hr after wire brushing. The welding must be completed within 24 hr after deoxidizing.

During production, a 3-weld, single-spot shear-test specimen for each interface and a 3-weld macro section is spot-welded cut and tested before starting to weld each joint. The average strength of the three single-spot shear specimens cannot be lower than 3% below the average strength obtained during certification of the joint being tested and none of the three may fail at a strength

Fig. 15 Roller spot welding skin to corrugation on a typical KC-97 floor panel



below the minimum single-spot strength obtained during certification.

The macro section (see Fig. 17) is cut, polished, etched and examined. Acceptable quality with respect to penetration and internal defects must be indicated. After examination, the specimen is cut into a $^{3}/_{32}$-in. wide strip and there can be no evidence of cracking as this strip is bent through 45 deg across the face of the weld as shown in Fig. 17.

These shear tests and macro tests in addition to those at the beginning of each joint are also required during production of Class SP spot welding at each of the following conditions:

1. Within $^{1}/_{2}$ hr after the beginning of each shift.
2. At intervals not to exceed 2 hr for each sheet thickness combination on each machine set up or at the end of the run if less than 2 hr has elapsed since the last test.
3. At each change in machine setup or change in machine settings.
4. Upon replacement of the welding electrodes.
5. At each change of alloy, condition or sheet thickness being welded.

In addition, the ultimate tensile strength is checked for the combination having the minimum total thickness of pile-up and the combination having the maximum total thickness of pile-up at 3-month intervals on each machine and each shift on which the machine is used in production. Five tensile test specimens are tested and the requirements are the same as those outlined for certification. The purpose of this quarterly test is to check the machine operation and the operators.

In developing a certified welding schedule for Class A spot welding, a 20-weld panel is welded, cut and tested in single-spot shear. The shear strength average must be equal to or greater than the minimum average specified and no spot may fail at a value below the minimum

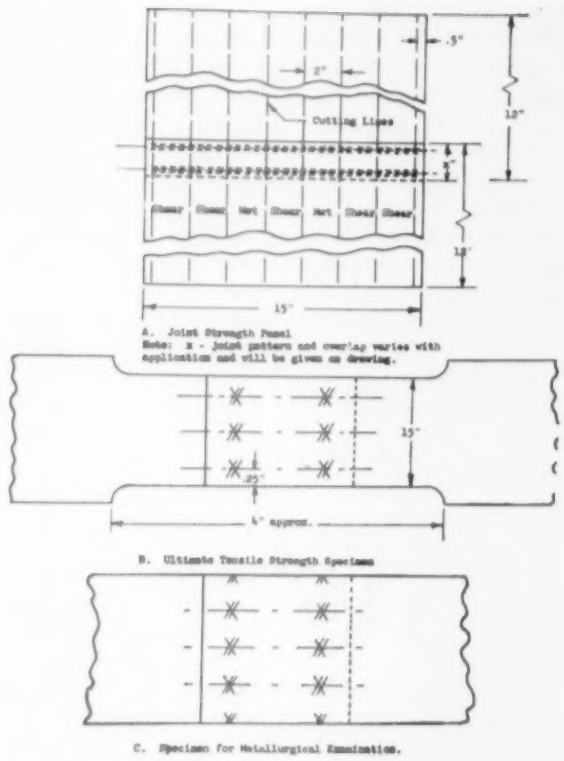


Fig. 16 Joint tensile specimens for qualification of "special-purpose" spot welding

single-spot strength specified. (See Fig. 4.) The consistency again must be such that 18 of the 20 welds fail within $\pm 12^{1}/_{2}\%$ of their average and the remaining two welds cannot fail at a strength within $\pm 25\%$ of the average of the 20 welds.

A five-spot section made on an actual or simulated

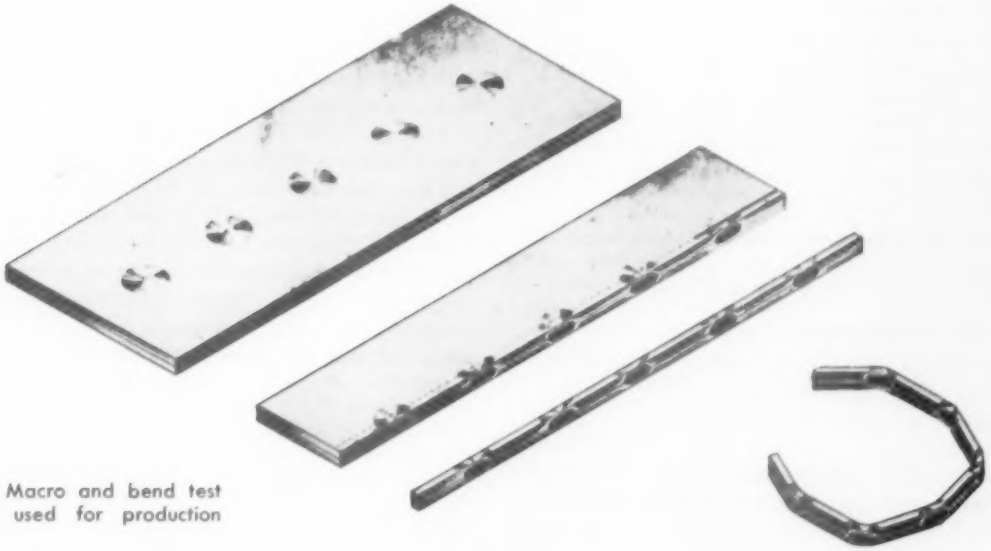


Fig. 17 Macro and bend test specimen used for production control

section of the part to be welded is welded, cut, mounted, polished, etched and examined for metallurgical soundness. The penetration must be within 30 to 80% of the sheet thickness and there can be no evidence of cracks, voids, porosity, metal expulsion, etc. The welds must be capable of meeting the bend test of the macro section as outlined for the special-purpose welding.

During production, shear tests are used as an index to demonstrate that the spot-weld machine is functioning as it was at the time of certification. A three-weld panel is welded, cut and tested and the average shear strength of these welds cannot be more than 5% below the average established during certification. These shear tests must demonstrate a consistency such that the variation does not exceed 0.35 when calculated by the formula:

$$\text{Variation} = \frac{\text{Range}}{\text{Average}} = \frac{\text{Difference between the highest and lowest shear strength}}{\text{Average of the three single-spot shear specimens}}$$

None of the spot welds, however, can fail at a strength below the minimum specified for the material thickness being welded.

The production shear tests for Class A spot welding are made for the same conditions as Class SP spot welding with the exception of those at the beginning of each joint.

A three-weld production macro section is welded, cut and examined for internal defects and to determine that the penetration is within the allowable limits of 30 to 80%. Here again, after examination the specimen is recut into a $^{3}/_{32}$-in. wide strip as shown in Fig. 17. As was the case for the SP welding, there can be no evidence of cracking as the strip is bent through 45 deg across the polished face of the weld. The macro section is made for the following conditions:

1. Within $^{1}/_{2}$ hr after the beginning of each shift.
2. At the middle and within $^{1}/_{2}$ hr of the end of the shift or after approximately 5000 welds, whichever is the lesser during continuous operation on the same machine setting or setup.
3. At each change of alloy, condition or sheet thickness being spot welded.
4. At each change in machine setup or change of machine settings. The macro sections are made on actual or simulated sections of the parts being welded.

The surface preparation for Class A spot welding includes chemical cleaning and deoxidizing. The surface resistance cannot exceed 100 microhms.

Economical Advantages

It may seem that such controls make the process very uneconomical. It is true that spot welding would be much cheaper if qualification certification and production testing could be eliminated. However, even with this handicap, spot welding is an economical joining process because of the rapid rate at which the welds can be made.

As an example, the average cost of assembling and spot welding on the 21 aforementioned body panels is approximately $0.022 per spot. This cost includes the shearing of the skins, layout and assembly time, the cost of cleaning the component parts, setting up the spot-welding machine, making the required production test and the actual spot welding.

It is quite obvious that on this application rivets, for example, could not be installed for this price. This is true particularly when it is considered that the external surface must be flush for aerodynamic smoothness and each fastener must be a seal because these panels are used on a pressurized fuselage.

From this it can be seen that there are two inherent advantages of spot welding. These are flushness and sealing. A third inherent advantage is that spot welds join two or more components without adding any weight. This is a very important factor in aircraft design.

It is not the intent to belittle other joining processes but rather to point out that spot welding is the most economical fastening method on certain applications and that very good joint strengths, quality and consistency can be obtained.

It would not be economically feasible to design for spot welding where only a few fasteners are required per assembly unless the quantity of parts is large. The real economy lies in those applications which require many fasteners so that the cost of surface preparation and process control may be low per spot weld.

Future Test Work

A test program is currently being conducted to determine the fatigue life of spot-welded joints. It is too premature to make any definite conclusions but trends indicate that fatigue life of a special purpose joint is increased by the addition of a third row of spot welds although the third row does not indicate any appreciable increase in static joint strength.

As an example in an 0.40- + 0.040-in. butt splice with an 0.064-in. splice plate, the fatigue life, based on the stress level of a particular design, with 2 rows of spot welds at 1-in. row spacing and $^{1}/_{2}$-in. spot spacing varied from 33,000 to 52,000 cycles. In the same combination with three rows of spot welds at $^{1}/_{2}$-in. row spacing and $^{1}/_{2}$-in. spot spacing, the fatigue life varied from 66,900 to 142,800 cycles. The static strengths by comparison were 51,400 and 52,400 psi, respectively.

A test program is also planned to determine the effect of the internal structure of the spot weld on fatigue life. This test is planned to compare fatigue strength of welds which contain internal cracks as indicated by radiography; welds which radiography indicates are crack free but will not meet the aforementioned bend test; and welds which will meet the bend test requirement.

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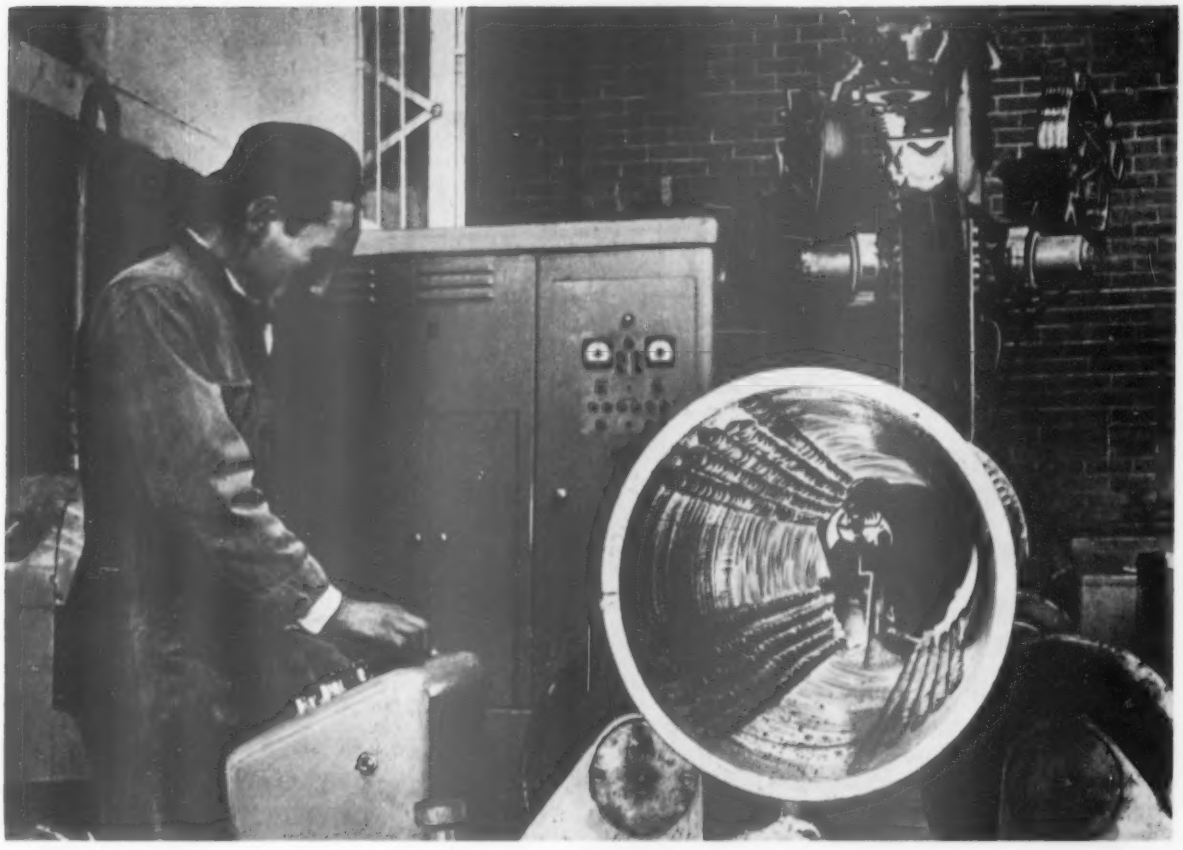

Fig. 1 Applying high-chromium iron to catalytic lift pipes by submerged-arc welding

SURFACING WITH COMPOSITE TUBE ROD

BY R. P. CULBERTSON

Preblended metal powders, in a continuously formed mild steel tube, used for automatic hard-facing and surfacing applications

ABSTRACT. Hard facing and surfacing of both new and worn components have gained increased usage throughout industry over the past decade. Most of the alloys suitable for hard-facing applications are produced in the cast form, suitable only for application by the oxy-acetylene or manual metallic-arc-welding processes. Since these alloys do not possess the mechanical properties necessary for production in the form of wrought coils of wire and rod, it had not been possible previously to adapt them to automatic metallic-arc-welding operations.

To meet the demand of industry for materials suitable for automatic welding processes, composite tube rods were developed. These tube rods consist of preblended metal powders in a continuously formed mild steel tube. This type material may also be produced in the form of coils and cut lengths suitable for oxy-acetylene welding. When coated, they are amenable to metallic-arc welding.

It has been possible by use of this type rod to obtain deposit chemistries tailored for the application. Fortified stainless steel compositions compensated for metallic burn-out, low chromium-molybdenum-carbon alloys, high-chromium irons, and other complex compositions are a few examples of these "tailor-made" compositions.

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Introduction

Welding has long been an accepted method of reclaiming worn components, providing wear-resistant overlays to both new and reclaimed parts and, in some

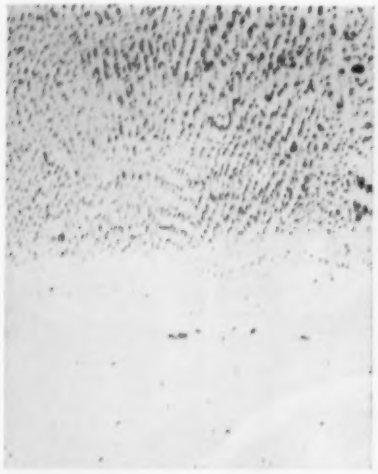

Fig. 2a First layer deposit of high-chromium iron rod

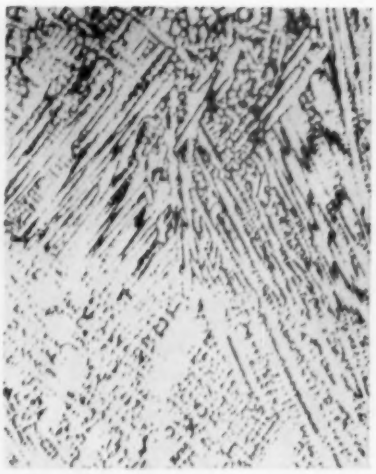

Fig. 2b Second layer deposit of high-chromium iron rod

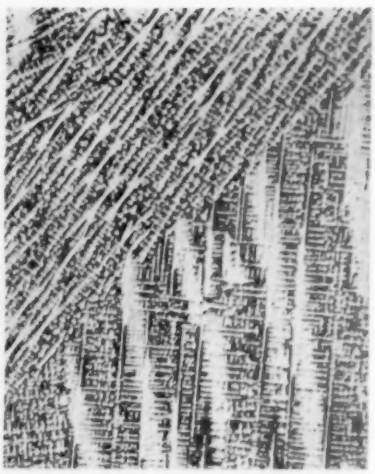

Fig. 2c Third layer deposit of high-chromium iron rod

instances, providing overlays with heat- or corrosion-resistant properties. With the advent of automatic welding processes, there has been increased demand for materials with chemical compositions exhibiting certain physical and mechanical properties, such as wear and abrasion resistance, impact resistance, work-hardening capacity, corrosion resistance and hardness. However, because of the physical and mechanical properties of certain of these alloys, it has been possible to furnish them in a cast form only. For this reason, the alloys were suitable only for manual metallic-arc or oxy-acetylene methods of deposition.

To provide these materials in a form amenable to automatic welding requirements, it was necessary to develop composite tube rods. The tube rod products are usually made on a tube mill, wherein a continuously formed steel tube is filled with various percentages of metal alloy powders. These filler powders are prepared by blending together various commercially available powdered metals and alloys, such as ferrochromium, ferromanganese and other materials of this type, in ratios required to produce the desired chemical analyses. The heterogeneous mixture is then fed into the tube in the proper sheath-to-filler weight ratio required to produce a desired analysis in a deposit.

It is usually necessary to incorporate a binder with the filler material which can be set by heat. This binds the filler material securely within the tube and prevents sifting of the filler from the tube during handling, shipping and subsequent use.

Material Control

In order to maintain a uniform material quality standard, it is necessary to use metal powders of approximately the same grain size and nearly the same density to avoid separation or segregation of materials in the powder-blended form. Therefore, the blending time, type of blending equipment and material are extremely important. Care must be exercised when conveying the material to the tube filling station and when filling the tube to avoid material segregation. Proper production techniques are also required when filling the tube to produce uniformly filled rod free of voids.

General Utility

To understand these materials better, it is necessary to examine some of the advantages, process limitations and typical materials available.

The advantages of composite tube rods are realized by obtaining special compositions, not otherwise available in wrought form, that provide special properties to the parts or base material to which they are applied. These materials, because of special composition, are, in general, more economical and, in many instances, are replacing the relatively more brittle cast materials. The tube rods can be readily applied by the automatic welding methods, both metallic-arc and oxy-acetylene. These materials are also available as stick-type electrodes for arc welding, and as bare rods for oxy-acetylene applications.

Table 1—Composition of Several Types of Composite Tube Rod Submerged Melt Deposits

	Element, %								Hardness,
Typical	C	Cr	Mn	Si	Mo	W	Fe	Ni	avg
1	0.60	5.00	0.80	...	0.80	...	Bal.	...	45–60
2	1.00	9.00	0.60	...	1.15	...	Bal.	...	52–60
3	2.75	27.50	0.65	...	...	...	Bal.	...	44–52
4	1.00	14.00	4.00	...	...	...	Bal.	...	28–35
5	3.00	17.00	...	...	16.00	...	Bal.	...	56–62
6	0.75	7.50	...	1.00	1.00	1.75	Bal.	...	44–49

The manufacturing process is generally limited by the amount of filler material that can be contained in the tube. This limits the alloy composition that can be obtained to about 50% of the total filled rod weight. However, for special high-alloy compositions, nickel, nickel-chromium alloys and various other alloy sheet materials may be used as the tube.

The most popular types of materials available in these forms are the martensitic low-chrome irons varying in chromium content from 2 to 10%.

Tungsten, molybdenum, vanadium, carbon, silicon, boron and other elements are added in combinations and amounts necessary to secure the desired results (see Table 1).

High-Chrome Iron Alloy Rod

Materials of this type have high cold-hardness, wear and oxidation resistance. Because of the chromium level (27%) in this material, it may be used successfully in service up to temperatures of 1100° F (see Fig. 2). The structure of this alloy consists of primary iron-chromium carbides in a matrix of iron-chromium solid solution and secondary iron-chromium carbides. The photomicrographs represent one-, two-, and three-layer deposits. The effects of dilution on the deposit structure are quite apparent. The factor of dilution is extremely important and should be carefully considered when selecting a surfacing material. Materials of this type (see Table 2) have excellent wear-resistant properties since the structure contains chromium in the carbide form. Chromium carbide is one of the most important wear-resistant ingredients and a common ingredient in the majority of hard-facing materials.

This alloy is recommended for surface overlays subjected to moderate impact and severe abrasion such as gyratory and gyrospheric crushers, pipe sizing dies, catalytic lift pipes and flaking rolls. The flaking roll shown in Fig. 3 weighs 2300 lb, with a 4-ft face, and a 2-ft diam. This roll was surfaced with a high-chrome iron alloy by the submerged-arc process. The surface was crack-free after welding and grinding.

This alloy has also been used for surfacing catalytic lift pipes which are subjected to abrasion and erosion at operating temperatures up to 1100° F. This deposition is usually performed by submerged-arc welding as shown in Figs. 1 and 4. Figure 5 shows a section of surfaced pipe. Note the dilution with the base material and the surface appearance.

Since this alloy contains high percentages of chromium and carbon, it is necessary to preheat the part to be surfaced to approximately 600° F to eliminate or minimize cracking during welding. The part should be shielded from drafts during the welding operation. Should the parts be extremely large, such as the roll shown in Fig. 3 it may be necessary to stress relieve at 1300 to 1350° F for a proper time followed by a slow furnace cool.

Fig. 3 Flaking roll hard-faced high-chromium iron by submerged-arc process

Fig. 4 Close-up of the hard-facing of catalytic lift pipes

Fig. 5 Cross section of hard-faced deposit on catalytic lift pipes

Table 2—Composition of High-Chromium Irons

	%				*Hardness, avg*
	C	*Mn*	*Cr*	*Fe*	
Nominal Weld:	2.75	0.65	27.50	Bal.	..
1st layer	1.70	1.72	18.34	Bal.	43–45
2nd layer	1.86	1.84	20.51	Bal.	45–50
3rd layer	2.02	1.88	22.75	Bal.	45–50

Fig. 6 Submerged-arc deposit of medium chromium-manganese-iron rod

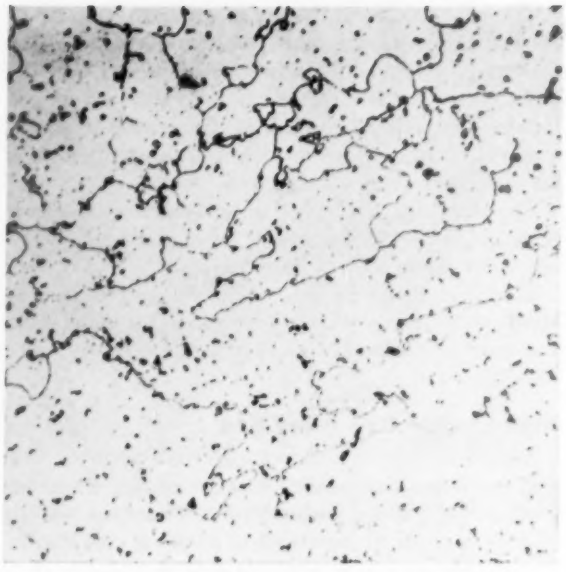

Fig. 7 Metallic-arc deposit of medium chromium-manganese-iron rod

Medium Chrome-Manganese-Iron Alloy Rod (Work-Hardening)

A medium chromium-manganese-iron, work-hardening type alloy, with analysis similar to that shown in Table 3 is designed for applications where extreme impact and moderate abrasion are encountered. This material excels in impact crusher and pulverizer service.

The photomicrographs shown in Figs. 6 and 7 are of submerged-arc depositions. This alloy, when deposited, retains austenite on normal air cooling from welding temperature. The austenite thus formed is unstable. On deformation or cold-working, cementite precipitates from the solid solution to produce additional hardness. This material has a deposit hardness of Rockwell-C 28 to 35 and work-hardens to Rockwell-C 45. Typical applications are dipper and drag-line bucket lips, teeth,

Fig. 8

Hard-facing by submerged-arc welding the spout area

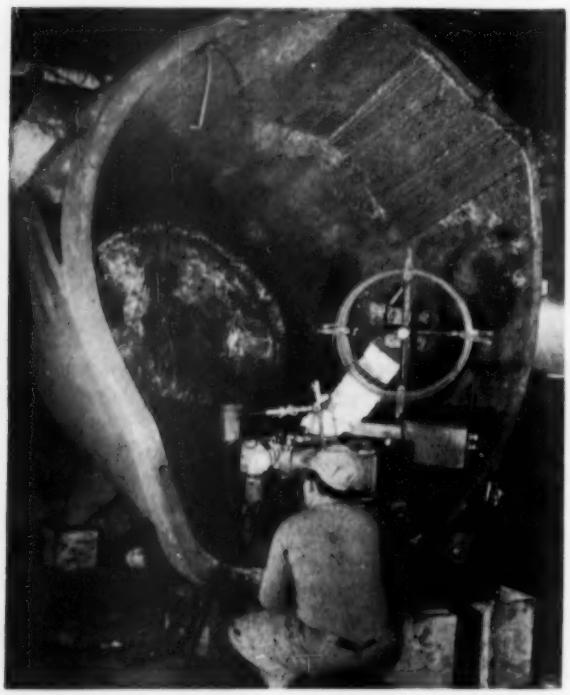

Fig. 9 Copper ladle after hard-facing was completed

Table 3—Composition of Medium-Chromium-Manganese-Iron Rod (Work-Hardening)

	%				Hardness, avg	Work hardness, avg
	C	Mn	Cr	Fe		
Nominal Weld	1.00	4.50	14.00	Bal.	28–35	42–48
1st layer	0.64	4.20	12.31	Bal.	24–29	...
2nd layer	0.66	4.70	15.30	Bal.	25–30	...
3rd layer	0.62	4.90	15.53	Bal.	27–34	...

latches, tractor and earth mover treads, links and pins, idler rolls, rollers, track pads, etc.

A particularly interesting application of this alloy is shown in Figs. 8 and 9. This is a 23-ton ladle used to pour molten copper. This ladle was surfaced with the chrome-manganese-iron alloy by the submerged-arc process. The ladle was faced over the bottom surface, 6 in. up the sides, and in the pouring spout areas.

The bottom of the ladle was covered by rotating the ladle while holding the welding machine stationary. The welding speed was 7 ipm. The spouts were surfaced by laying the ladle on its side and moving the welding machine back and forth in 2-ft sweeps along the spout area. The $^3/_{16}$-in. thick deposit on all lined surfaces required about 900 lb of chrome-manganese-iron tube rod.

The ladle was surfaced to retard the severe eroding effect of molten copper when poured into the ladle and out over the spouts.

Medium and Low Chromium-Molybdenum-Iron Alloy Rods

The medium and low chrome-molybdenum-iron alloys (see Tables 4 and 5) generally produce deposit hardnesses of Rockwell-C 45 to 60. The deposits are usually martensitic with some untransformed austenite (see Figs. 10 and 11).

The resistance of these materials to abrasion, erosion and galling is very good although the resistance to impact is rather low. These factors depend on the hardness of the martensitic deposit. As hardness increases, abrasion resistance increases and impact resistance is lower. Since these materials contain from 5 to 10% chromium, they have fair resistance to scaling; however, they should be used only on jobs involving low temperatures since the hardness of these materials drops rapidly as the temperature increases.

Table 4—Composition of Low-Chromium-Iron Rod

	%					Hardness avg
	C	Mn	Cr	Mo	Fe	
Nominal Weld	0.60	0.80	5.00	0.80	Bal.	...
1st layer	0.42	0.92	2.82	0.54	Bal.	45–47
2nd layer	0.51	1.04	3.78	0.66	Bal.	57–58
3rd layer	0.57	1.14	3.99	0.76	Bal.	57–59

Table 5—Composition of Medium-Chromium-Iron Rod

	%					Hardness, avg
	C	Mn	Cr	Mo	Fe	
Nominal Weld	1.00	0.60	9.00	1.00	Bal.	...
1st layer	0.53	1.60	7.17	1.01	Bal.	52–54
2nd layer	0.61	1.80	8.25	1.29	Bal.	54–60
3rd layer	0.60	1.80	9.22	1.35	Bal.	54–60

Table 6—Composition of Chromium-Molybdenum-Tungsten-Iron Alloy

	%					Hardness avg
	C	Cr	Mn	W	Fe	
Nominal Weld	0.85	7.60	1.10	2.20	Bal.	...
1st layer	0.61	5.01	0.70	1.00	Bal.	54
2nd layer	0.66	7.22	1.02	1.54	Bal.	52
3rd layer	0.75	8.64	0.96	1.62	Bal.	50

These materials have found wide usage for building up tractor rollers, tractor idlers, sheave wheels, roll necks, roll faces, scraper blades and other applications requiring good cold-abrasion properties.

Chrome-Molybdenum-Tungsten Alloy Rod

This alloy is an example of the special composition type material, previously mentioned, that may be obtained by using composite tube rod. The small amount of tungsten added to this composition (see Table 6) imparts added abrasion resistance.

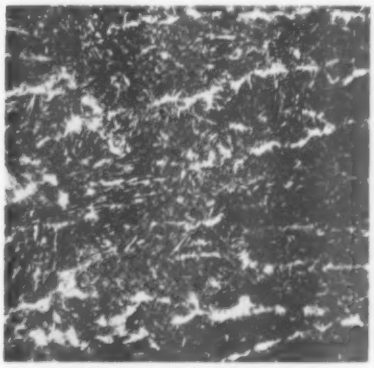

Fig. 10a First layer deposits of low chromium-molybdenum-iron rod

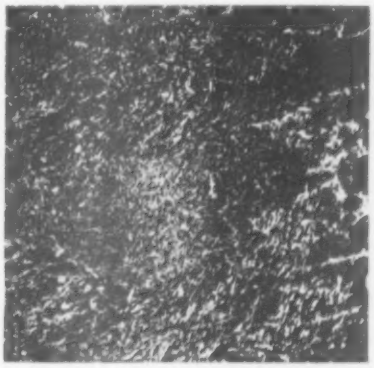

Fig. 10b Second layer deposits of low chromium-molybdenum-iron rod

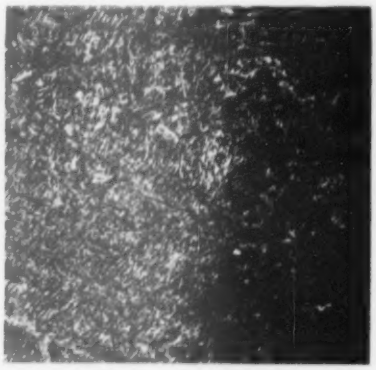

Fig. 10c Third layer deposits of low chromium-molybdenum-iron rod

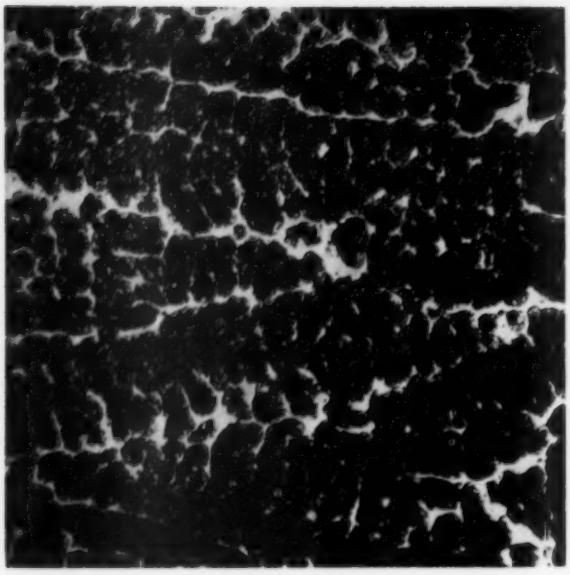

Fig. 11a First layer deposit of medium chromium-molybdenum-iron rod

Fig. 11b Second layer deposit of medium chromium-molybdenum-iron rod

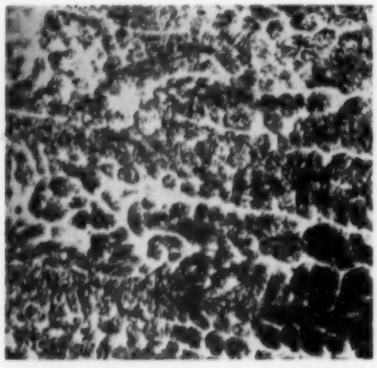

Fig. 12a First layer deposits of chromium-molybdenum-tungsten alloy rod

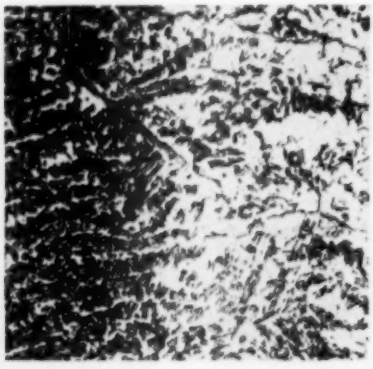

Fig. 12b Second and third layer deposits of chromium-molybdenum-tungsten alloy rod

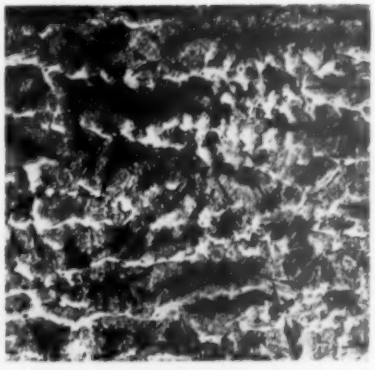

Fig. 12c Third layer deposits of chromium-molybdenum-tungsten alloy rod

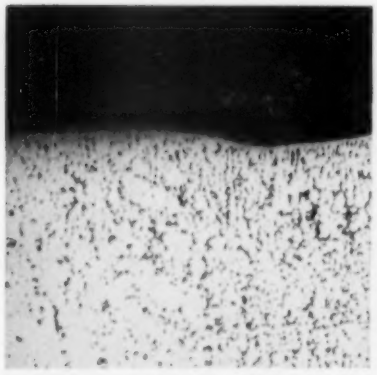

Fig. 13a First layer deposits of chromium - molybdenum - cobalt - carbon alloy rod

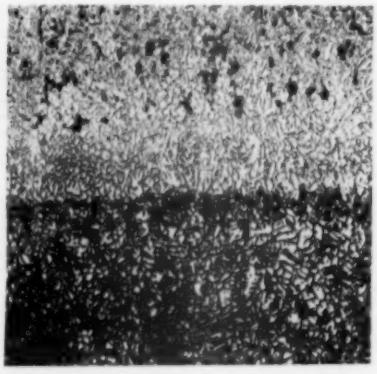

Fig. 13b Second layer deposits of chromium - molybdenum - cobalt - carbon alloy rod

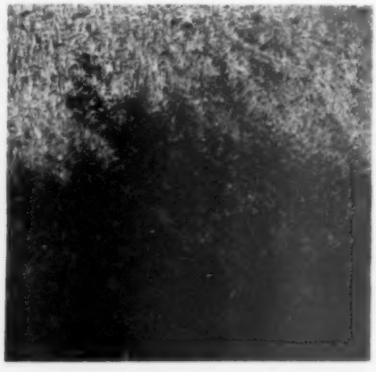

Fig. 13c Third layer deposits of chromium - molybdenum - cobalt - carbon alloy rod

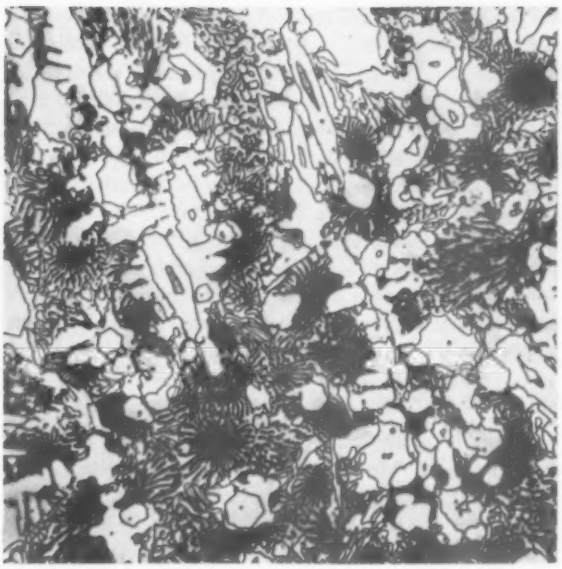

Fig. 14 Typical microstructure of a gas deposit of chromium-molybdenum-cobalt-carbon alloy rod

Photomicrographs of a deposit show a white ferritic matrix with dark tempered martensite and pearlite in the first layer (see Fig. 12*A*). The weld junction of the second and third layers (see Fig. 12*B*) has a microstructure in the second layer, at the right, of white ferrite and dark tempered martensite with pearlite. A third layer, at the left, consists of an austenitic matrix and martensitic precipitates. The third layer deposit (see Fig. 12*C*) near the surface has an austenitic matrix (white) with dark martensitic precipitates.

This material has been used as an abrasion-resistant overlay on tool joints, crane wheels, shovel idlers and coiler rolls.

Chrome-Molybdenum-Cobalt-Carbon Alloy Rod

This is another example (see Table 7) of a very complex material that may be produced as a composite tube rod. This material is suitable to handle severe abrasion with only moderate impact as might be encountered in sand and gravel equipment.

The resistance to abrasion is brought about by high carbon and chromium with the addition of tungsten and/or molybdenum to give resistance to frictional heat and strength at high temperature. Cobalt and/or nickel is added to increase the resistance to corrosion

Table 7—Composition of Chromium-Molybdenum-Cobalt-Carbon Alloy Rod

	%						*Hardness, avg*
	C	*Cr*	*Mo*	*Co*	*Fe*	*Mn*	
Nominal Weld	3.00	17.00	16.00	5.00	Bal.	...	...
1st layer	1.32	11.17	11.84	4.42	Bal.	1.14	52–56
2nd layer	1.58	14.33	15.30	5.61	Bal.	1.20	56–59
3rd layer	1.68	15.58	15.94	5.94	Bal.	1.21	56–60

Fig. 15 Mechanized application of tungsten carbide tube rod to hammer mill hammers by the oxy-acetylene process

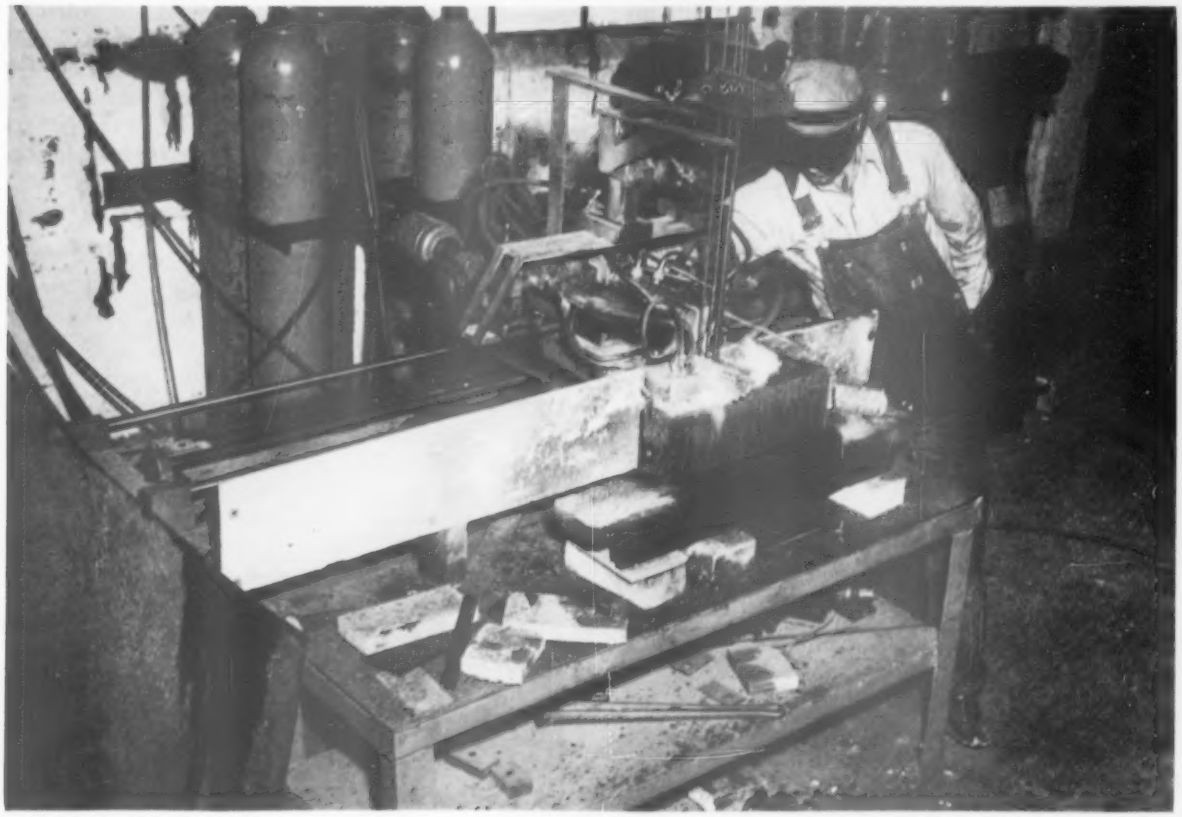

Fig. 16 Installation of the hard-faced hammers in a hammer mill

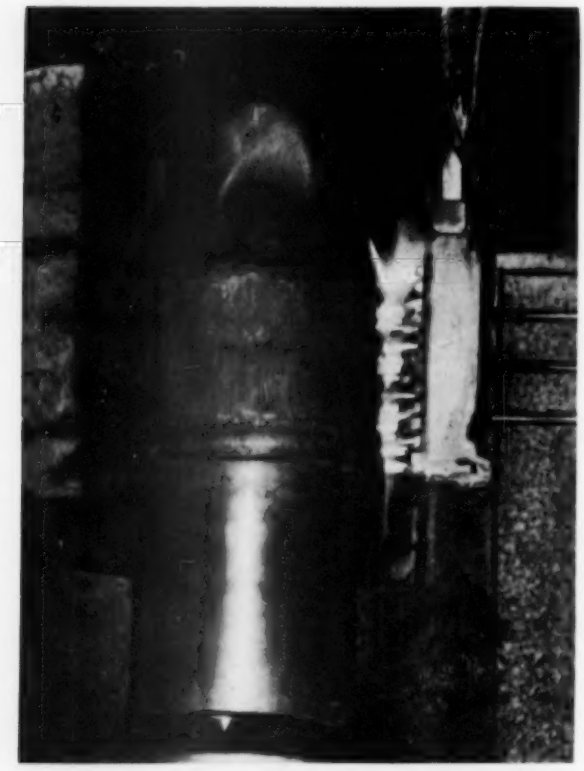

Fig. 17 Hard-faced band of tungsten carbide being applied to oil well tool joints

and oxidation at high temperatures. This very complex composition is unique in high "as-deposited" hardness (Rockwell-C 62) with an ultimate strength of 40,000 psi and a compressive strength of 400,000 psi. Figure 13 is an example of the deposited (submerged arc) microstructure of the first, second and third layers. Figure 14 is an example of the microstructure of a gas deposit with this alloy. The white areas are primary chromium carbides, with dark areas of M_6C in an iron-chrome-cobalt solid solution and secondary chrome carbides. This material has found wide usage in the hard facing of plowshares, pug mill pusher shoes, cement chutes, fan blades and pump impeller blades.

Tungsten Carbide Rod

One of the more familiar types of composite tube rod is the mild steel tube filled with granulated tungsten carbide particles. These rods are produced in forms suitable for oxy-acetylene, metallic-arc and automatic metallic-open-arc applications. These materials are furnished in various weight percentages of crushed tungsten carbide such as 60% of crushed tungsten carbide to 40% mild steel tube. The rod is made in various tungsten carbide mesh sizes such as 10 on 20, 20 on 30, 30 on 40, 40 on 60, 60 on 100, 30 on 100 or other sizes as needed.

To obtain the most wear-resistant deposit and subsequent retention of tungsten carbide particles in the deposit, the oxy-acetylene welding process is used. Tungsten carbide composite tube rod may be applied by the manual metallic-arc or automatic metallic-arc process, but with reduced tungsten carbide particle retention. Due to penetration, force of the arc and high density of the granular material, most of the tungsten carbide particles are found near the bottom of an arc deposit.

This material has found wide usage for hard facing of hammer-mill hammers, tool joints, rotary drill bits, plowshares and other applications subjected to severe abrasion.

Several methods of applying tungsten carbide composite tube rod have been devised for flat and cylindrical applications. Figure 15 shows a mechanized application to tungsten carbide tube rod, by the oxy-acetylene process, to hammer-mill hammers. These hammers are usually stacked side by side in lengths up to 3 ft. The hard-facing material is applied to the full width of the part by using a multiple-flame welding head and utilizing an oscillating movement of the welding head during the linear traverse of the parts. The parts are hard faced on both ends and then broken apart. Figure 16 shows the installation of hard-faced hammers in a hammer mill.

Figures 17 and 18 show the application of tungsten carbide composite tube rod to tool joints, utilizing the mechanized cylindrical oxy-acetylene equipment for this deposition. Figure 17 shows a hard-facing band while Fig. 18 shows the hard-facing material being applied to grooves in the tool joint.

The submerged-arc welding process is one of the most

widely used for field reclamation of tool joints, collars and other drilling equipment. If the shoulder of the tool joint is worn badly, it is usually rebuilt using a suitable high-tensile solid rod and the submerged-arc welding process. A build-up bead is then applied to the preheated joint (600° F). The bead is applied in a manner to simulate a thread helix with a double lead (see Fig. 19). This bead is applied by the submerged-arc process utilizing a 4.00 to 7.00% chromium-bearing composite tube rod. The tungsten carbide composite tube rod is then applied in the groove between the beads (see Fig. 20). The material is applied by the open-arc process to provide good retention of carbide particles in the weld deposit. By depositing the tungsten carbide in the groove formed by the weld beads, the deposit and tungsten carbide particles are kept above or at the surface of the tool joint. This minimizes wear on the joint. Figure 21 shows a tool joint with the weld deposits worn down.

Summary

From the foregoing discussion, we may draw a comparision between composite tube rod alloy compositions and wrought or cast alloy rod compositions. The most outstanding advantages of the composite rods are:

1. Special alloy compositions are available.
2. Composite rods are usually more economical than comparable cast rods.
3. The tube rods can be adapted to automatic welding processes.

Although tubular rods of special or high-alloy compositions may be produced inexpensively, it must not be construed that they will compete with solid wire such as Type 420 stainless steel, high-carbon steel wire, or any wrought alloy composition which can be easily drawn into wire.

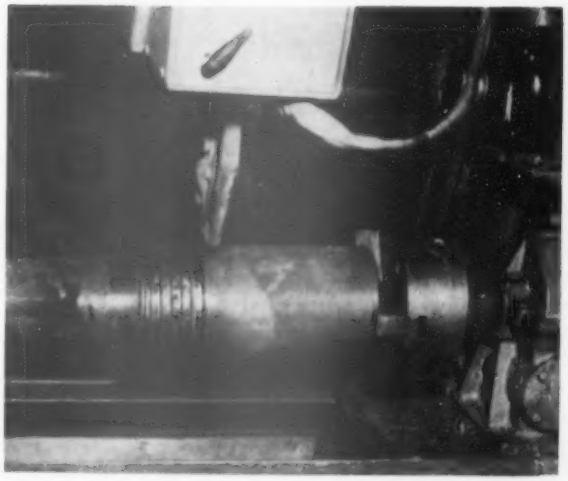

Fig. 19 The bead is applied in a manner simulating a thread helix with a double lead

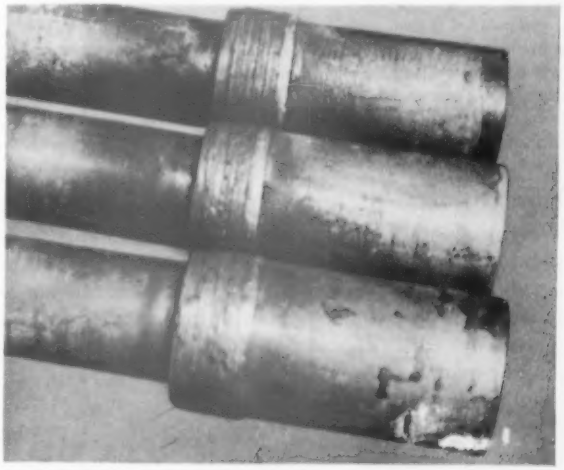

Fig. 20 Tungsten carbide is applied to grooves between the buildup beads shown in Fig. 19

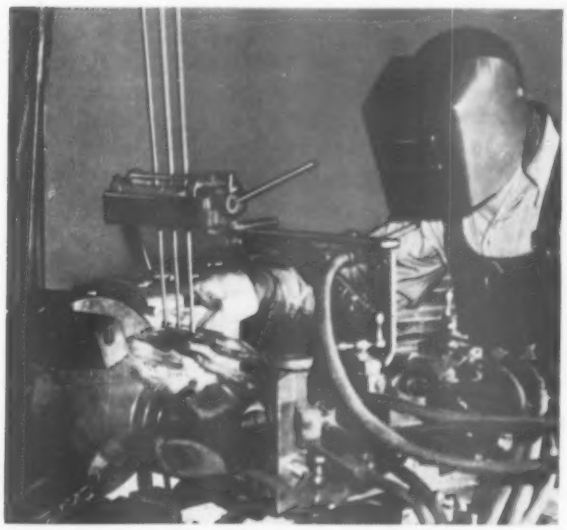

Fig. 18 Setup for mechanized facing of oil well tool joints

Fig. 21 Here is a tool joint with the weld deposits worn down. Note the tungsten carbide particles

EVALUATION OF FUELS AND OXIDANTS FOR WELDING AND ASSOCIATED PROCESSES

Authors propose an improved method for comparing and evaluating fuel-oxidant combinations for gas heating applications

By W. B. MOEN AND J. CAMPBELL

ABSTRACT. An improved method for the comparison and evaluation of fuel-oxidant combinations for gas heating applications is suggested. The product of normal burning velocity and the heating value of the combustible mixture is shown to be a more significant parameter than flame temperature. A number of methods of computation of this factor called "combustion intensity" and their interpretation are discussed. Experimental verification of the computed comparative values is presented.

Introduction

The selection of a particular fuel gas-oxidant combination to meet specific heating requirements is based principally upon technical and economic considerations. It is the purpose of this paper to propose an analytical method by which the performance of various systems may be made and to offer experimental evidence of the validity of the method. The use of the computed performance of a number of systems then will allow one to resolve the economic considerations.

Of principal technical interest is the ability of a burning fuel-oxidant system to heat a workpiece to a desired temperature in the minimum time. By desired temperature is meant not only the surface temperature at the point of flame application, but also the temperatures of the surface at distances away from the point of application and the temperature profile within the body of the workpiece. The last-mentioned factor is principally a function of the thermal conductivity of the material and not of the combustion system. The configuration of the surface temperature map is due to a combination of the thermal conductivity of the work and the condition of the secondary flame reaction products.

The premise of minimum time required to heat a localized area to an elevated temperature is applicable to any gas-heating process and is only a matter of degree whether it be welding, oxygen-cutting preheat, hardening, etc. However, the factors mentioned above govern the rate of heat input which should be a maximum, and the time of heat application which should be a minimum.

Basically, the problem is resolved into how the energy from an open flame is transferred to the workpiece surface. Six mechanisms have been suggested as follows:

1. Forced convection from the combustion products to the work.
2. Radiation from the flame to the work.
3. Exothermic catalytic combination of active species on the workpiece surface.
4. Catalytic combustion on the solid surface.
5. Transfer of excess energy from high energy gas molecules to the solid by collision.
6. Exothermic equilibrium displacement.

The last four of these items have been shown to exhibit little effect. Radiation transfer has been measured, and contrary to popular opinion, contributes as a maximum only about 15% of the total available heat.[1] Consequently, the forced convection mode is controlling and any measurement of this parameter with different systems will be indicative of their performance for commercial use. One such parameter called "combustion intensity," or "specific flame output," will be described herein.

The economic comparison for any one application should include not only the cost of the gases themselves, but also the cost of the gas handling system, and a realistic appraisal of the labor chargeable to the application. Some systems which may satisfy both technical and economic requirements may be precluded from use by other factors such as toxicity and corrosiveness of either the raw gases or the combustion products.

Flame Temperature

The thought that the performance of fuel-oxidant systems can be evaluated by flame temperature long has been held. However, if the effect of temperature on both convection and radiation heat transfer is ex-

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amined, it can be seen that temperature is a secondary and not a controlling factor.

Figure 1 illustrates the adiabatic flame temperature of a number of fuel gases burned with oxygen over a wide mixture range as computed by the method of Hottel, Williams, and Satterfield.[2] For acetylene and propane, each burned at its metallurgically neutral ratio, i.e., approximately 50 and 20% by volume of fuel, respectively, the temperatures are 5420 and 5190° F, respectively. In the case of radiant energy interchange, if it is assumed that all factors in the Stefan-Boltzman equation except temperature remain the same with relation to each other during each heating cycle, the heat transfer will be dependent only upon the difference of temperatures of the flame and work, each raised to the fourth power. If it is assumed that the workpiece is originally at 70° F and will be heated to 2000° F, the following proportionality will hold:

		Acetylene	*Propane*
(*a*)	$\left[\frac{\text{Flame temp (° R)}}{100}\right]^4$	1195×10^4	1020×10^4
(*b*)	$\left[\frac{\text{Work temp at start (° R)}}{100}\right]^4$	0.08×10^4	0.08×10^4
(*c*)	$\left[\frac{\text{Work temp at finish (° R)}}{100}\right]^4$	36.6×10^4	36.6×10^4
(*d*)	$a - \frac{b + c}{2}$	1177×10^4	1002×10^4

Now, taking acetylene as the base, the propane use results in a radiant driving force only 14.9% lower than acetylene. When it is realized that only a maximum of 15% of the energy transferred is by radiation, the difference between the fuels is only 2.2%.

If forced convection now is considered, the temperature difference enters as the first power and for propane, if all other conditions are held constant, only about 5% less energy will be transferred.

Consequently, the argument based solely upon temperature considerations is seen to be fallacious and one must look for another measure to compare different fuels.

Combustion Intensity

The quantity "combustion intensity" or "specific flame output," has been advanced by others as a superior measure of the performance of a combustion system than flame temperature. It is loosely defined as the product of the heating value of the mixture and some function of the normal burning velocity of the mixed gases. It will be noted that the composite term is a measure of the rate of heat release per unit area which justifies its use as a measure of convective heat transfer. Those systems having few mols of products produced upon combustion generally have a high burning velocity, e.g., oxy-acetylene and oxy-hydrogen, and consequently release their energy over a small area. This advantage may be offset in some cases by a low heating value of

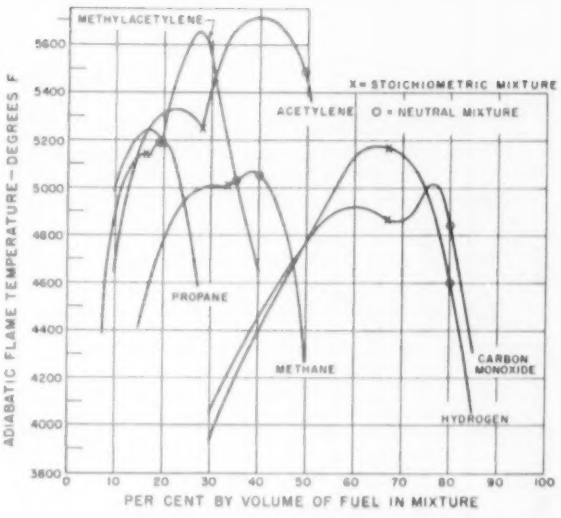

Fig. 1 Calculated adiabatic flame temperature of various fuel gas-oxygen mixtures

the mixture which is determined by both the net calorific value of the gas and the mixture ratio, e.g., oxy-hydrogen.

A number of interpretations of combustion intensity have been described in the literature. For example, the definition by Smith[3] considers only the total flame performance with no distinction between primary and secondary combustion. Furthermore, he suggests that fuels should be compared only at the point of maximum burning velocity. Consequently, his definition may be written as

Total combustion intensity =
Max normal burning velocity ×
Net heating value of mixture at the composition corresponding to maximum normal burning velocity

It is believed that maximum flame speed mixture ratios do not represent a basis for comparison of fuel-oxidant systems because, in practice, these mixture ratios seldom are used.

Passauer[4] and Kunz[5] both define combustion intensity in terms of over-all flame output but do not confine their interpretation to the point of maximum burning velocity. Kunz attempts to evaluate the fuels at mixture ratios with oxygen when used for oxygen-cutting preheat. However, some of his values of usable mixture ratios, notably oxy-propane, are suspect.

Bruckner and Lohr,[6] and Perry[7] who summarizes their work, take cognizance of the difference between primary and secondary combustion intensity, whose sum equals the total combustion intensity. However, one big difference in the definition is that Bruckner and Lohr define combustion intensity in Btu per square foot of port area per second, whereas the other sources define it as Btu per square foot of primary cone area per second. The burning velocity is utilized by Bruckner and Lohr but is corrected by a factor equal to the ratio of cone area to port area. This conversion, in

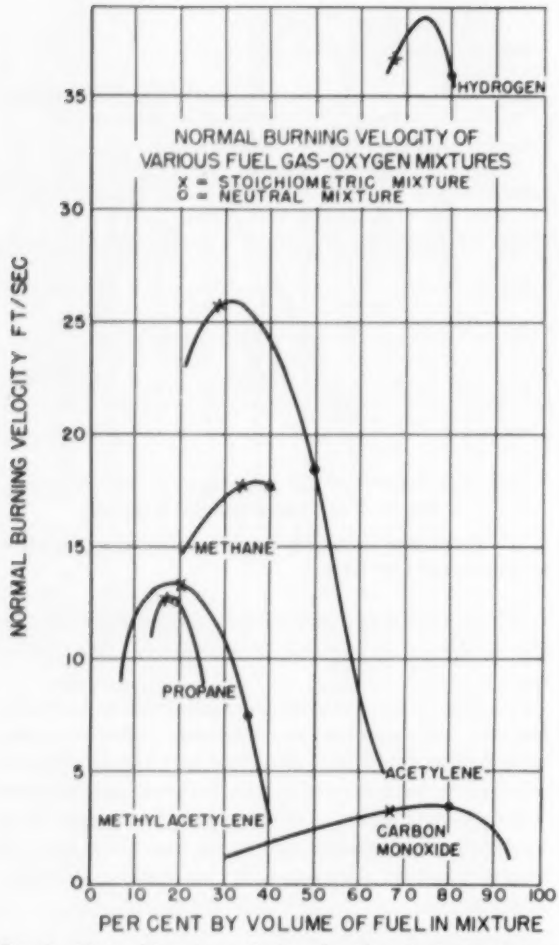

Fig. 2 Normal burning velocity of various fuel gas-oxygen mixtures

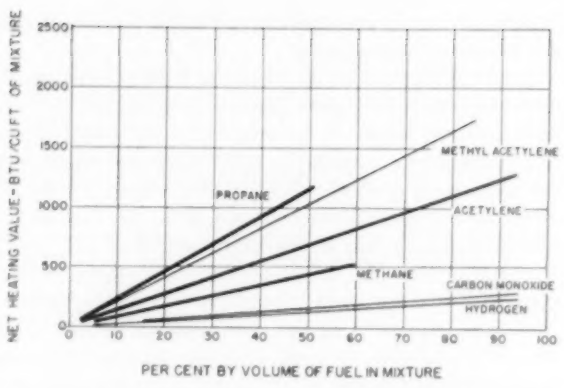

Fig. 3 Total net heating value of premixed systems within flammable range

effect, evaluates the flames partially in terms of exit orifice gas velocity. This method does have the advantage of attempting to define the importance of exit velocity and eliminates the cone height effect but does introduce an equipment dependency factor. In addition, in determining heating value of the mixture going to primary combustion products, Bruckner and Lohr use only an approximate method.

To reconcile the above mentioned differences in definition and interpretation, both primary and total combustion intensities for the range of mixture ratios for which the basic data are available are presented herein. It is believed that values based upon flame area are more fundamental than port area and for this reason a flame area basis has been used. The following relationships have been used:

(*a*) Primary combustion intensity =
Normal burning velocity ×
Net heating value of the mixture going only to primary cone combustion products

(*b*) Total combustion intensity =
Normal burning velocity ×
Net heat value of the mixture going to complete combustion

(*c*) Secondary combustion intensity =
Total combustion intensity −
Primary combustion intensity

One will observe that all fuel-oxidant combinations have a full range of combustion intensities that may overlap each other depending upon the mixture ratios considered. The only limitation to this observation is the flammability limits of the gases under consideration. It will be noted below that these limits are so wide that they never will be approached in practice.

Flammability Limits of Fuel Gases in Oxygen

	Percent by volume	
	Lower	*Upper*
Carbon monoxide	15.5	93.9
Methane	5.4	59.2
Hydrogen	4.7	93.9
Propane	2.5	51.0
Acetylene	2.5	93.0
Methylacetylene	..	..

The mixture ratios of greatest industrial interest are those that are metallurgically neutral to the workpiece, only slightly oxidizing or carburizing, again with reference to the work, and stoichiometric.

The information presented in the remaining portions of this section evaluates the individual factors entering the composite combustion intensity definition.

Normal Burning Velocity

Normal burning velocity is a fundamental combustion property and is defined as the velocity with which a plane flame front moves normal to its surface through the adjacent unburned gas. A number of methods for measurement of burning velocity are available, but disagreement on results obtained makes the selection of the absolute values difficult. The values of normal burning velocity used in this paper were those obtained by the visible cone method. The data are shown in Fig. 2.

Heating Value

The computation of the three previously defined combustion intensities requires the use of the appropriate

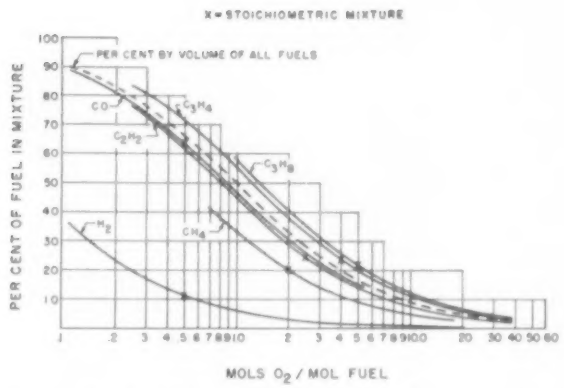

Fig. 4 Gravimetric composition of various fuel gas-oxygen mixtures

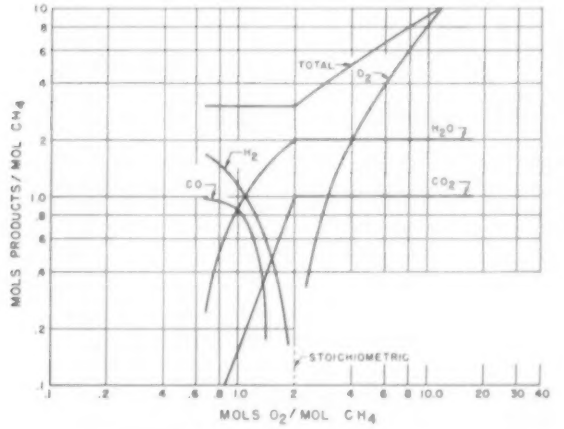

Fig. 5 Composition of products of inner cone of CH_4-O_2 mixtures

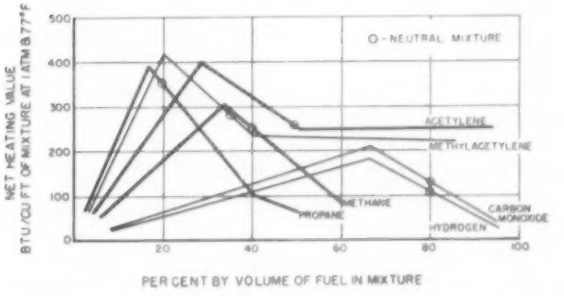

Fig. 6 Net heating value of various fuel-oxygen mixtures going to primary combustion products within flammable range

net heating value. The total combustion intensity requires the use of the net heating value of the entering mixture which is merely the product of the heating value of the fuel times the percent fuel in the mixture. The results of this calculation for the fuels of interest within the flammable range are shown in Fig. 3. Indicated in Fig. 4 is the composition, both volumetric and

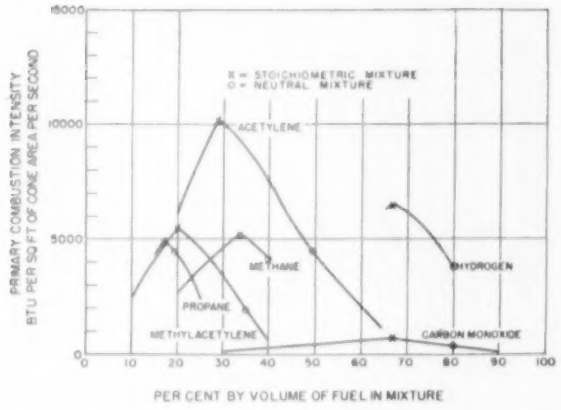

Fig. 7 Primary combustion intensity of various fuel gas-oxygen mixtures

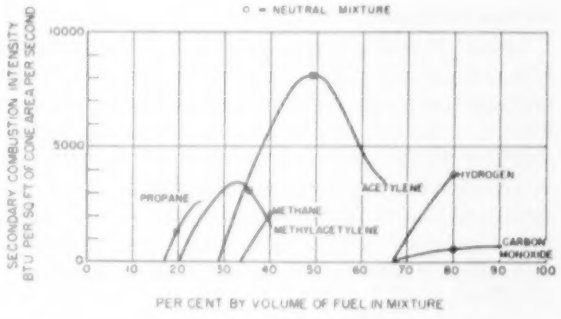

Fig. 8 Secondary combustion intensity of various fuel gas-oxygen mixtures

gravimetric, of the same fuels in oxygen as a function of fuel-oxidant ratio.

The computation of the heating value of the entering mixture going only to primary combustion first involves the calculation of the equilibrium composition of the primary cone products. The method of Hottel, Williams and Satterfield[2] may be applied to determine these intermediate products. Values for all of the fuel gases considered have been computed. A sample computation is shown for methane in Fig. 5. Two weaknesses of the source data were met. One is the consideration that only CO, CO_2, H_2, H_2O, O_2 and N_2 are active species in the CHON system, whereas the high-temperature oxidation reactions actually produce in addition to these products O, H, N, OH. Another weakness is that the upper temperature limit of the reference charts is below the flame temperatures encountered for most fuel-oxygen systems. However, work done by others, using more elaborate computational methods and experiment, has indicated that the results of both intermediate products and flame temperature are in surprisingly good agreement.

Once the intermediate products have been determined, the summation of the heats of formation of each of the products weighted according to the composition may be made. The results of this last set of calculations for all the fuels are shown in Fig. 6.

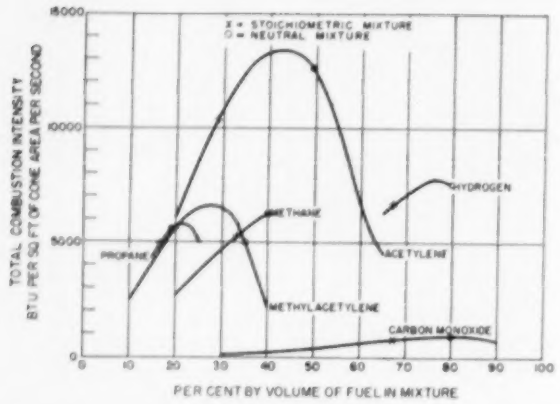

Fig. 9 Total combustion intensity of various fuel gas-oxygen mixtures

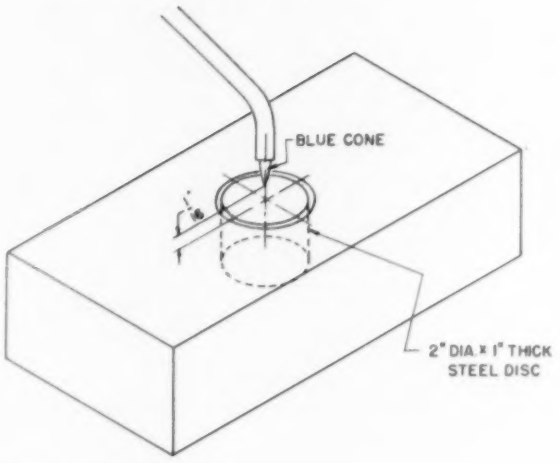

Fig. 10 Heating test

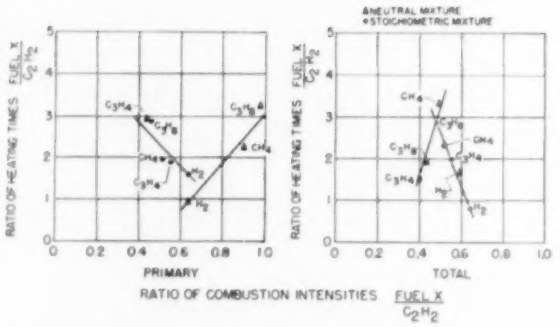

Fig. 11 Correlation of combustion intensity and heating time

Combustion Intensity

Levels of combustion intensities for all the fuels for wide ranges of mixture ratios were computed by the methods previously defined. The results are shown in Figs. 7, 8 and 9. Of greatest interest are the relative values of the fuel-oxidant mixtures at the stoichiometric and so-called neutral mixture ratios, the last term being defined as a mixture ratio which produces no oxidation or carburization on the surface of a steel workpiece. The values of percent by volume fuel for both ratios are as follows:

	Stoichiometric mixture	*Neutral mixture*	*Neutral fuel, %/ Stoichiometric fuel, %*
Acetylene	28.6	49	1.71
Propane	16.7	19	1.14
Methane	33.3	40	1.24
Hydrogen	66.7	80	1.20
Methylacetylene	20.0	35	1.75
Carbon monoxide	66.7	..	..

It will be noted from the last column that there is no consistent relation for the neutral mixture ratio in terms of the stoichiometric mixture ratio.

Experimental Verification

To determine which concept of combustion intensity is of use, a correlation between computed and experimental values was attempted. The experimental approach was based upon the premise that minimum heating time for a given heat input is required.

The equipment used is shown in Fig. 10. A 2- x $4^1/_2$- x 9-in. insulating brick was recessed to retain tightly a 2-in. diam by 1-in. thick disk of SAE 1020 steel. For each test specimen, surfaces were machine finished to a smoothness that would eliminate effects of tool marks. The various fuel gases and oxygen tested were obtained from commercial sources and in each instance they were reduced from source pressure and separately metered by fixed orifices before being admitted to the torch, mixer and tip assembly shown in Fig. 10.

For each test conducted the flame was played on the center of the disk and the time interval from the start of heating until the first indication of fusion was recorded. The primary cone tip-work separation was maintained at approximately $^1/_{16}$ in. It has been noted from previous laboratory work that small variations from this separation will not appreciably affect heating times.

Four series of tests were run as follows:

(*a*) Stoichiometric mixture ratio
(*b*) Neutral mixture ratio
} Assuming complete combustion

(*c*) Stoichiometric mixture ratio
(*d*) Neutral mixture ratio
} Assuming only primary combustion products

For series (*a*) and (*b*) a total heat input of 10,000 Btu per hour released by complete combustion for each fuel-oxidant system was obtained by adjusting individual gas flow rates. For series (*c*) and (*d*), a heat input of 2500 Btu per hour going only to primary combustion products was experimentally obtained in the same manner. In all four tests the tip orifice had to be changed to maintain stable flame conditions. In this work the neutral ratio has been considered to be the maximum

Table 1—Results of Heating Time Tests

End product condition	*Fuel*	*Mixture*	*Heat input, Btu/hr*	*Tip orifice diam, in.*	*Heating time, sec.*	*Combustion intensity, flame area, Btu/sec/sq ft*
Complete	Acetylene	Stoichiometric	10,000	0.076	19	10,200
	Hydrogen	Stoichiometric	10,000	0.076	15	6,530
	Methylacetylene	Stoichiometric	10,000	0.086	38	5,650
	Methane	Stoichiometric	10,000	0.086	44	5,270
	Propane	Stoichiometric	10,000	0.086	54	4,925
	Carbon monoxide	Stoichiometric	10,000	*	*	
Complete	Acetylene	Neutral	10,000	0.076	39	12,700
	Hydrogen	Neutral	10,000	0.076	65	7,570
	Methylacetylene	Neutral	10,000	0.086	59	5,100
	Methane	Neutral	10,000	0.086	129	6,250
	Propane	Neutral	10,000	0.086	75	5,500
	Carbon monoxide	Neutral	10,000	*	*	
Primary	Acetylene	Stoichiometric	2,500	0.067	101	10,200
	Hydrogen	Stoichiometric	2,500	0.076	162	6,530
	Methylacetylene	Stoichiometric	2,500	0.067	190	5,650
	Methane	Stoichiometric	2,500	0.067	197	5,270
	Propane	Stoichiometric	2,500	0.067	289	4,925
	Carbon monoxide	Stoichiometric	2,500	*	*	
Primary	Acetylene	Neutral	2,500	0.067	148	4,600
	Hydrogen	Neutral	2,500	0.076	136	2,850
	Methylacetylene	Neutral	2,500	0.067	430	1,930
	Methane	Neutral	2,500	0.067	330	4,170
	Propane	Neutral	2,500	0.067	480	4,500
	Carbon monoxide	Neutral	2,500	*	*	

* No heating times obtainable even when using a No. 12 (0.161-in.) tip.

oxygen-fuel gas ratio whose primary combustion products are metallurgically neutral to the workpiece, i.e., do not visibly oxidize or carburize the top surface. Conceivably, there are other applications of these fuel gas-oxygen systems for which a neutral ratio would be slightly different. The average results of a large number of runs for each test condition are shown in Table 1.

Discussion of Results

A graphical correlation of computed combustion intensities, both primary and total, with heating times for stoichiometric and neutral mixture ratios is shown in Fig. 11. Any fuel-oxidant system may be taken as the base but since acetylene generally resulted in shortest heating times, it was selected. It can be seen, with but two exceptions, that excellent correlation was obtained for the consideration of either primary or total combustion intensity. For both exceptions, since the heating times were determined accurately by experiment, adjustment could be made only in the combustion intensity term. In this term only the burning velocity can be considered and since widely different values are given in the literature, the ones selected may be erroneous.

Since both primary and total combustion intensity have shown agreement with experiment, either one may be considered valid for comparison of fuel-oxidant systems. However, a consideration of primary and secondary combustion intensities will indicate the apportionment of total heat to localized and diffuse heating for specific applications. The fuels considered have the following values at the neutral mixture ratio:

	Combustion intensity, flame area, Btu/sec/sq ft			*Flame temperature, ° R*
	Primary	*Secondary*	*Total*	
Acetylene	4600	8100	12,700	5880
Hydrogen	2850	4650	7,500	5060
Methyl-acetylene	1930	3170	5,100	5500
Propane	4500	1000	5,500	5650
Methane	4170	2080	6,250	5520
Carbon monoxide	400	700	1,100	5290

It is noted that acetylene appears the highest of all the fuels for both conditions considered. If, for some reason, a fuel other than acetylene is selected, consideration should be given to the type of heating since some of them have higher secondary combustion intensities than primary values and should be applied to diffuse heating applications.

From the table above it can be seen that the fuels rated by flame temperature do not fall in the same order as either combustion intensity or heating times.

Conclusions

The agreement between computed and experimental results indicates that combustion intensity is a valid method for estimating the performance of both existing and proposed fuel-oxidant premixed systems. In addition, it explains on a firm basis the superiority of oxy-acetylene over other fuel gas-oxygen systems for both localized and diffuse heating applications.

The computation of combustion intensity also serves as a method to estimate the performance of previously

untried fuel-oxidant systems. A number of acetylenics burned with oxygen have been proposed,[8] but unfortunately the basis of comparison is flame temperature. Other hydrocarbon-oxygen and hydrocarbon-fluorine[9] mixtures also have been suggested but no comparative indication of heating ability has been put forth. A computation for each system of the heats of reaction plus either a calculated or measured normal burning velocity will permit the computation of combustion intensity. These quantities may then be evaluated and used to determine if a proposed system is both technically and economically attractive.

Acknowledgment

The authors wish to express their appreciation to their colleagues for their helpful assistance, and to Air Reduction Co., Inc., for its permission to present this paper.

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WAYS IN WHICH AMERICAN WELDING SOCIETY SERVES ITS MEMBERS

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SHOP METHODS FOR IDENTIFYING SOME METALS PRIOR TO WELDING

ARTHUR L. PHILLIPS

When a maintenance welder is called in, he is rarely told the composition of the metal involved which will permit him to select the correct alloy to use on the base metal.

It is comparatively easy to discover if a metal is cast iron or steel, but to differentiate between the various steels requires study and experience. There is a great difference in the welding technique involved and the welding alloys recommended will depend upon the composition of the base metals.

In production operations, the need for identification may not arise because the composition of the base metal is known. It is essential, however, that a rapid, easy way of identifying metals be known by maintenance welders.

Fortunately, simple methods have been devised for testing metals which are available to all welders. The results will not be as comprehensive as laboratory testing, but may enable the operator to identify the metal.

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Fig. 1 The approximate hardness of metals can be determined by the file test. When the file bites deeply, the metal is relatively soft. When the file slides over the surface without resistance, the metal is hard. The varying hardnesses will indicate the group to which the metal belongs

Hardness Test

The degree of hardness possessed by steel has a distinct effect upon its weldability. Furthermore, if the Brinell hardness is known it is possible to calculate the tensile strength because the tensile strength is approximately the Brinell hardness number multiplied by 500.

The file test is a useful method of determining hardness (see Fig. 1). It will not give scientific accuracy but it will enable the operator to determine the relative hardness of a piece of metal and its machinability.

A new machinist's hand file should be used for the purpose and kept for nothing else if much testing is necessary. The following table indicates this method of testing:

Brinell hardness no.	*Resistance to file*	*Type of steel*
100	High resistance. File bites into metal.	Low-carbon steel (mild)
200	High resistance. File bites into metal, but more pressure is needed.	Medium-carbon steel
300	Medium resistance. File does not bite into metal and more pressure must be added.	High-alloy steel
400	Low resistance. Metal can be filed but with difficulty.	High-carbon steel
500	Resistance practically nil. Can be marked but metal is practically as hard as the file.	Tool steel
600	No resistance. Metal harder than file. No grip whatsoever.	Hardened tool steel

A more accurate instrument for determining hardness can be constructed with a steel ball-bearing and a glass tube (see Fig. 2). This will give scientific accuracy and has the additional advantage of not defacing or marking the metal tested. It is based upon the principle of the Shore Scleroscope.

When a steel ball is dropped upon a piece of metal it will bounce. The height of the bounce will determine the hardness.

If the ball is held at the top of the glass tube and dropped upon a number of pieces of known Brinell hardness and the glass tube calibrated to show the height to which the ball bounces, the instrument becomes an accurate means of measuring hardness.

Fig. 2 When a steel ball bearing is held at the top of a glass tube and dropped upon pieces of metal, of known Brinell hardness, the height to which the ball bounces indicates the hardness of the metal. If the glass tube is calibrated it becomes an accurate instrument for measuring hardness

The file test is useful for determining whether a metal is soft or hard, machinable or nonmachinable, but much experience is necessary before the intermediary degrees of hardness can be estimated with any accuracy.

Chip and Spark Tests

The chip test is an easy way of identifying certain metals. When the metal is chipped with a cold chisel the size and characteristics of the chips will vary with different metals.

The spark testing of metals is a useful way of classifying them because any deviation from the known spark stream of a metal will indicate the presence of "foreign" materials.

When a piece of metal is held against a power grinding wheel, small particles are torn away (see Figs. 3 and 4). These are removed so rapidly that they become incandescent. The pattern of the spark stream differs to some extent with each metal and with the varying alloying elements.

Identification by Uses

Metals may be identified by their operational uses. For instance, certain machine parts may be cast iron; others, steel forgings. Automobile carburetors are usually die castings. Shafts are normally made of steel of varying carbon content, and the same applies to machine tools.

Many metals can be identified readily by their appearance and color. This may be the first step in grouping them into broad classifications.

Cast Iron

This term generally includes pig iron, white cast iron, malleable iron and gray iron.

Many welders have difficulty in recognizing the various types of iron and steel and should a mistake be made, the resulting weld will fail or will not possess the desired characteristics.

White Cast Iron

This metal is hard, brittle and magnetic (a simplified magnetic test is shown in Fig. 5). It is readily broken by a sharp blow with a hammer and the fracture will appear silvery and white.

When white cast iron is chipped the chips will be smooth, very brittle and about $^1/_8$ in. in size. The fracture has a fine silvery white crystalline formation and the unfinished surface is dull gray in color with evidence of the sand mold. The metal is so brittle that the chips appear as small broken fragments leaving a rough edge scored with indentations.

The chips melt at a moderate speed, becoming dull red before melting. A medium slag film develops and the molten puddle is fluid, watery and reddish white. Under the flame the puddle remains quiet with no sparks.

The spark test will show a very small volume of red sparks at the wheel, turning to straw color with an average stream length of 20 in. The sprigs are small and repeating.

Malleable Cast Iron

Malleable cast iron is easily recognized by the color and composition of its fracture. It has a white, steely skin extending from the surface toward the center. The center of the casting is usually dark and dull. The chip test will show rough and tough chips of about $^1/_4$ in. to $^3/_8$ in. in size.

This metal melts at a moderate speed, becoming red before melting. A medium slag film develops and the molten puddle is fluid, watery and of a straw color. The puddle boils under the flame and leaves blowholes. The surface of the molten metal sparks but the interior does not.

The sparks are straw yellow when the metal is held against a power grinding wheel. The shafts are longer than those from gray iron. They are approximately 30 in. in length with a moderate volume and ending in numerous small, repeating sprigs. The metal is magnetic.

Gray Cast Iron

This is the most common form of cast iron encountered. It is used extensively in machinery castings. The fracture will be of a dark gray color and if a finger is rubbed across the surface of a newly made fracture it will be smudged by the graphite.

Chips melt at a moderate speed becoming dull red before melting. The slag forms a thick skin and the molten puddle is fluid, watery and reddish white. The puddle is quiet under the flame and no sparks are emitted.

The sparks from the grinding wheel are red, turning to straw color with an average stream length of 25 in. The volume is small with many small repeating sprigs. The metal is magnetic.

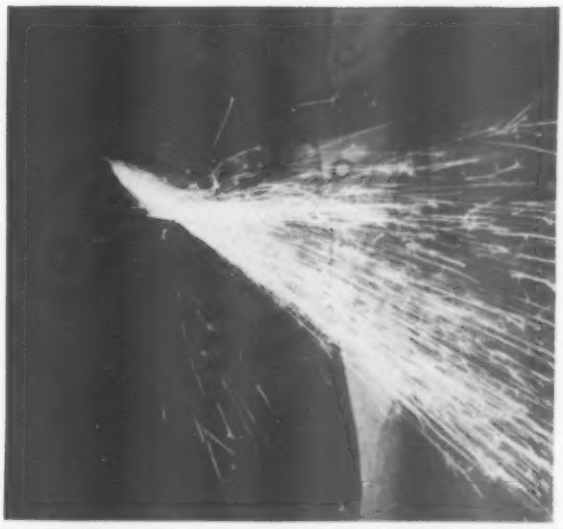


Fig. 3 The spark test is not a substitute for chemical analysis but it is an excellent method of checking the type of metal. The points to study in a spark stream are: the carrier lines, their size and color, if continuous or disjointed. If standard specimens of known composition are kept on hand, a comparison of the sparks would permit very accurate deductions to be made

Chips will be smooth, brittle and about 1/8 in. in size.

Steel

Cast Steel

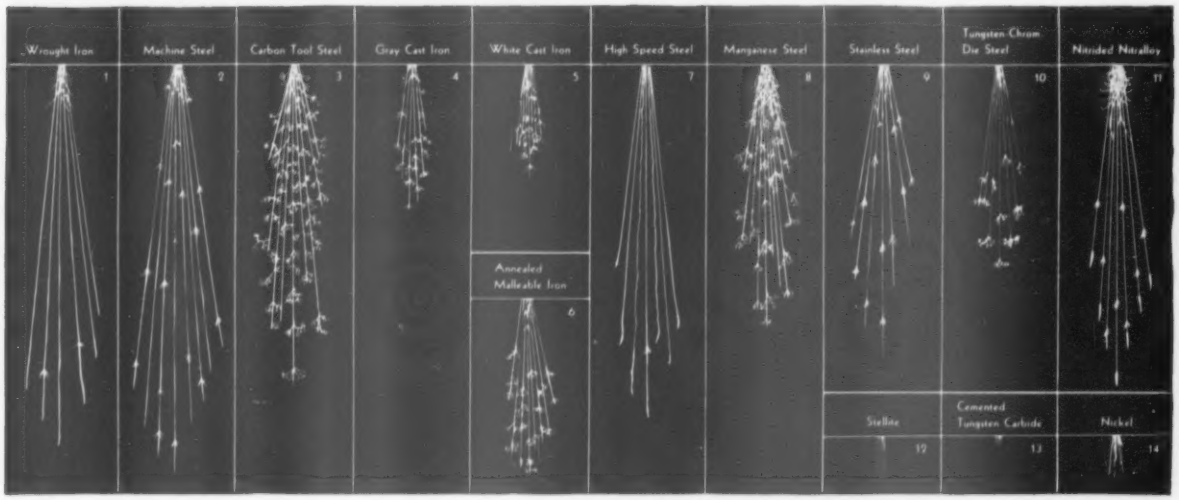
This is used for machine parts requiring higher strength than can be obtained with cast iron. The application may thus identify the material. The fracture is bright gray and the unfinished surface dark gray with probable evidence of the mold. When newly machined it is very smooth and a bright gray color.

The metal is easily chipped and the chips will have smooth edges and can be continuous if desired.

When the flame test is applied the chips will melt rapidly. They become a bright red before melting, and the slag appears similar to the molten metal. The molten metal is straw colored and emits sparks under the torch flame.

A spark test will show white sparks. The average length of the stream will be about 70 in., depending upon the pressure, with a moderately large volume. The shafts will be short with forks and appendages. The forks become more numerous as the carbon content increases.

Characteristics of Sparks Generated by the Grinding of Metals



Metal	*Volume of Stream*	*Relative Length of Stream, Inches†*	*Color of Stream Close to Wheel*	*Color of Streaks Near End of Stream*	*Quantity of Spurts*	*Nature of Spurts*
1. Wrought iron	Large	65	Straw	White	Very few	Forked
2. Machine steel	Large	70	White	White	Few	Forked
3. Carbon tool steel	Moderately large	55	White	White	Very many	Fine, repeating
4. Gray cast iron	Small	25	Red	Straw	Many	Fine, repeating
5. White cast iron	Very small	20	Red	Straw	Few	Fine, repeating
6. Annealed mall. iron	Moderate	30	Red	Straw	Many	Fine, repeating
7. High speed steel	Small	60	Red	Straw	Extremely few	Forked
8. Manganese steel	Moderately large	45	White	White	Many	Fine, repeating
9. Stainless steel	Moderate	50	Straw	White	Moderate	Forked
10. Tungsten-chromium die steel	Small	35	Red	Straw*	Many	Fine, repeating*
11. Nitrided Nitralloy	Large (curved)	55	White	White	Moderate	Forked
12. Stellite	Very small	10	Orange	Orange	None	
13. Cemented tungsten carbide	Extremely small	2	Light Orange	Light Orange	None	
14. Nickel	Very small**	10	Orange	Orange	None	
15. Copper, brass, aluminum	None				None	

†Figures obtained with 12" wheel on bench stand and are relative only. Actual length in each instance will vary with grinding wheel, pressure, etc. *Blue-white spurts. **Some wavy streaks.

Fig. 4 This chart shows some of the spark patterns obtained when a metal is held against a grinding wheel. (Courtesy Norton Co., Worcester 6, Mass.)

Low-Carbon Steel

The degree of hardness is a good indication of the carbon content in steel. The harder the steel the greater the carbon content.

When a file test indicates hardness up to Brinell 300 the metal is low to medium-carbon steel. Above this rating the steel is in the high-carbon classification.

Other characteristics are similar to those given for cast steel.

High-Carbon Steel

The fracture is a very light gray in color but the unfinished surface is dark gray and rolling or forging lines may be noticeable. When newly machined it is very smooth and a bright gray.

The chip has a fine grain structure with the edges lighter in color than low-carbon steel. The chip can be continuous if desired and, although the metal is usually very hard, it can be chipped without difficulty.

Under the flame test the metal melts very rapidly becoming bright red before melting. The appearance of the molten slag is similar to the molten metal.

When held against the grinder, the sparks are white with an average stream length of 55 in. There is a large volume with numerous small and repeating sprigs.

High-Speed Steel

This name is usually applied to alloy tool steel because of its composition. It has a high carbon content which will be disclosed by its hardness when file tested.

The stream pattern is totally different from high-carbon steels. The volume is small but the color is red close to the grinding wheel turning to a straw color toward the end of the stream. The spurts of sparks are few in number and of the forked variety.

Manganese Steel

When manganese is added to high-carbon steel there is an increase in tensile strength and the metal work-hardens in use.

This metal can be recognized easily by the magnet test. Manganese steel is nonmagnetic.

The spark stream is similar in many respects to that of carbon tool steel but about 10 in. less in length.

Stainless Steels

Steels in this category are stainless because of the chromium content of the steel. An easy way to discover if a steel belongs to the stainless classification is to prepare a solution of 94% wood alcohol and 6% nitric acid and apply a drop to the metal. If there is no discoloration in one minute's time the metal belongs to the stainless steel group.

Nonferrous Metals

Aluminum

Aluminum is a white, soft metal, very light in weight and the fracture is white and moderately fine grained. The unfinished surface is smooth and dark gray. When machined the surface is very smooth and a light gray in color.

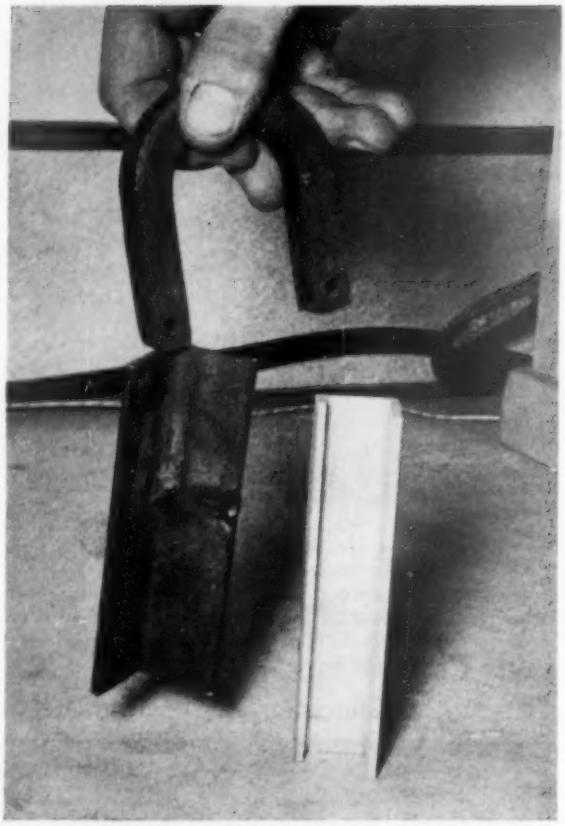


Fig. 5 A magnet is a useful instrument to have available. The magnetic qualities of a metal indicate the group to which it belongs

Aluminum alloys are harder and slightly darker than pure aluminum. The chips are easily cut, smooth and leave a saw edge where chipped. The chip could be continuous if desired. The metal melts faster than steel but with no visible change of color. The slag appears to be a thick black scum and the appearance of the molten metal puddle is similar to that of the unmelted metal. The molten metal under the slag has high fluidity with a quiet action under the torch.

Aluminum produces no spark when held against a grinding wheel and is not magnetic.

Magnesium

This is a light, hard white metal which has insufficient strength in its pure form to be of much industrial use, but which alloys readily with aluminum, manganese, zinc, etc. This produces alloys comparable in strength to aluminum, yet having only 65% of the weight.

It is nonmagnetic and aluminum is the only metal for which it is likely to be mistaken. When a torch flame is applied to filings of magnesium they will burn with a blinding white flame.

No spark test can be made because the metal gives off no sparks and is so soft that it clogs the grinding wheel.

Copper and Copper Alloys

In its pure state copper is a reddish brown metal and

is used in its almost pure state for electrical purposes. It is possible, therefore, to recognize this metal by its end use, because next to silver, it is one of the most electrically conductive metals known.

Pure copper is easily cut and has smooth chips which can be continuous if desired and show a saw edge where cut. It melts slowly compared to steel and usually turns black, turning to red as the copper color becomes more intense. There is so little slag that it is hardly noticeable. The molten puddle appears almost mirror-like directly under the flame and has a tendency to bubble.

Copper is nonmagnetic and does not spark when in contact with a grinder.

Copper alloys readily with at least 30 other elements, 17 of which are used in making commercial copper alloys. Zinc is used in proportions of from 1 to 50% in making approximately 15 different commercial brasses. Bronzes and brasses of widely different properties are produced by varying the proportions of the alloying elements.

Brass is usually yellow in color and formed into sheets, tubes, extrusions and bar stock. Bronze is reddish in color and usually cast.

Nickel

Nickel is a hard, white, malleable, ductile metal which is valuable for the alloys it forms with other metals. The fracture is coarse grained and almost white. The unfinished surface is smooth, dark gray in color and when machined leaves a very smooth, white surface.

It can be chipped easily and the chips will have smooth edges and can be continuous if desired. It melts slower than steel and becomes red before melting. The slag forms a gray scum and the molten metal is very fluid under the slag film.

When subjected to the spark test the sparks will be orange in color with an average length of 10 in. Shafts are short with no forks or springs. The metal is magnetic.

Monel

Monel is an alloy of 65 to 70% nickel with copper, manganese, iron, silicon and carbon.

The fracture is slightly darker than that of nickel but the other characteristics remain the same.

There is considerably more slag than is produced when nickel is melted. Monel is much less magnetic than nickel but is usually harder.

Inconel

Inconel contains a higher nickel content than Monel. An analysis shows approximately 78% nickel, 13½% chromium, 7% iron and small percentages of manganese, copper, silicon and carbon.

The easiest way to identify Monel is to use the acid test. Apply 1 drop of concentrated nitric acid to the metal and permit it to remain for 1 min. If there is no reaction the metal is Inconel. If the solution turns a cloudy blue-green color the metal is Monel.

Clad Metals

The trend toward cladding metals has been the cause of many wrong identifications. When steel is clad with aluminum or copper it is easy to mistake the part for the clad metal instead of the base metal.

The magnetic test should be used on nonferrous clad materials. The degree of magnetic attraction will be an indication of the composition of the base metal.

Conclusions

An expert spark tester can identify the constituents, and percentages, of an alloy with a high degree of accuracy. Every effort should be made to verify conclusions by using other testing methods to substantiate an opinion. The shape of the part may indicate it has been cast; its color may place it in a certain group; operating conditions may narrow the group to a metal classification and end use will determine the type of alloy needed to perform efficiently under service conditions.

The time and trouble spent in learning how to identify metals will be amply repaid by the ability to specify the correct alloy for a given metal-joining operation.

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WELDING OF CHROME-MOLY STEELS IN HIGH-PRESSURE HIGH-TEMPERATURE SERVICE

BY C. D. COOPER

A new arc-welding system has been developed for welding the chromium-molybdenum steels used in high-temperature, high-pressure service. The system is designed for reverse polarity, direct current welding in all positions.

The practical welder runs up against the chrome-moly steels in the power boiler field and in the fabrication and repair of equipment and piping for the petroleum refining and chemical processing industries. Welding these steels has, in the past, presented some problems because of their tendency to harden and become brittle upon air cooling from welding temperatures.

The system described here virtually eliminates the danger of underbead cracking, and reduces the normal preheat and postheating requirements for these steels. In order to assure consistently sound welds, however, the welder must do his part by observing a few simple precautions (see Figs. 1 to 7).

First, the new system is built around the use of a low-hydrogen electrode with closely controlled carbon content and a straight lime coating. Classified under AWS and ASTM specifications as Type XX15, these electrodes provide a weld metal deposit of controlled analysis and high ductility.

Choosing the right diameter is as important as selecting the right basic type. Recommended current ranges are somewhat higher for these electrodes than for conventional rods of the same size. For example, a $^1/_8$-in. diam., properly operated, requires about the same current input as a $^5/_{32}$-in. diam. commercial E-6010. In commercial practice there is a tendency to pick the largest rod which the work will support. When welding the chrome-moly steels it is better to pick the highest current that the work will support, and then select the electrode that will still function at this current level.

The joints should be carefully fitted and the welding groove carefully prepared. In view of the rather heavy slag volume of these electrodes, it is important that the root opening be wide enough to manipulate the arc and to insure good penetration. Where no root opening is provided the included angle should be not less than 90°. When a root opening of $^1/_8$-in. or more can be provided, the included angle may be 60° or less.

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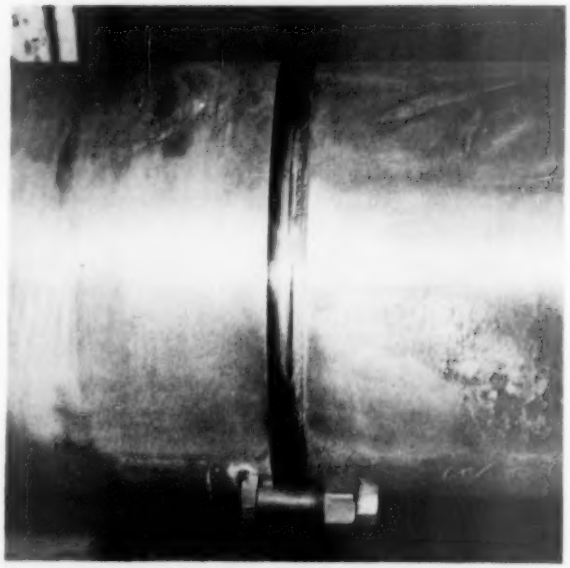


Fig. 1 Joint preparation is important. The included angle depends on the root opening, but must be large enough to manipulate the arc and control the rather heavy volume of slag

Fig. 2 Preheating of a weld area in the horizontal rolled position. Preheat temperatures as much as 200° F below normal may be used, in the new system

Fig. 3 Welding the first pass. Note that the electrode is held at a 90° angle to the work. Excessive tilting would permit air to enter the arc zone

Fig. 5 Ready for the final pass, just before the welder strikes his arc. The arc must be held as short as possible at all times to assure finest weld quality

The weld area must be carefully cleaned to remove all scale, oil or rust, because the accidental inclusion of any source of oxygen may serously interfere with the quality of the deposit.

In general, this system permits welding at preheats from 100 to 200° F below the temperatures normally used. For steels with lower chromium content, 300° F. is hot enough, except on very heavy sections. For the $2^1/_4$ chromium-1 molybdenum and 5 chrome-$^1/_2$ moly compositions, the preheat should be more carefully applied, but even here the welder is allowed more latitude than when using higher carbon electrodes. Preheat requirements for a given job depend upon the weld thickness and the geometry of the weldment involved. The heat-affected zone of the base metal will harden to the same degree, regardless of the ductility of the weld deposit, so that some preheating is always necessary.

There are three important requirements which the welder must observe—a very close arc, a rod angle perpendicular to the work, and a weaving technique in depositing the weld metal.

The close arc is necessary to avoid the introduction of air, with resultant difficulties from oxidation of the

Fig. 4 Proper application of these electrodes requires careful cleaning of the weld area in order to remove all slag or other foreign matter, if maximum weld quality is to be realized

Fig. 6 Grinding the weld flush, prior to X-ray examination. The work is rotated into position as grinding proceeds and the entire circumference will be ground

Fig. 7 Local postheat treatment furnace in position, ready for connecting the gas line and firing up. Many welds may be safely placed in service without stress relieving

weld puddle. The arc should be held as short as possible to assure maximum weld quality. Excessive tilting of the electrode will also create unsatisfactory results because tilting again permits the introduction of air into the arc zone. As a general rule, the electrode should be held at an angle of 90° to the work. This may at first appear difficult to accomplish in pipe work, but it can be done with a little practice.

The weld metal should be deposited with a weaving technique to retard the rate of cooling of the weld puddle. In order to avoid entrapment of slag, the weave should be limited so that the arc is not directed into solidified slag. The weave may be as wide as the welder can manage and still keep the slag molten across the crater area. The slag should be carefully cleaned from each bead before depositing the next bead in order to avoid including particles of slag.

Postheating requirements, too, are somewhat relaxed as compared to normal procedure on these steels. When working on steels in the lower range of chromium content, unless sections are extremely heavy it is unnecessary to maintain preheat after welding and prior to stress relieving. In fact, unless applicable codes require it, many weldments may be safely placed in service without stress relieving, when welded under this system.

Welded steels containing $2^1/_4$ chromium and up should always receive a postheat treatment. Industrial practice usually dictates a temperature of 1300-1400° F for stress relief on these compositions.

Electrodes used in this system deliver maximum results only when the coatings are perfectly dry. It is necessary to protect them in storage as well as in use from moisture pickup. They are shipped in moisture proof cans. As long as the can remains unopened, they are adequately protected. Once the can has been opened, however, the unused electrodes should be stored at a temperature somewhat above room temperature.

Following the procedures recommended under this system will enable the welder to produce consistently sound welds on chrome-moly steels. The heavy coating of the electrodes, and their low moisture level, keep spatter losses to an unusually low level. The stable arc action assures a weld bead with fine ripples, free from porosity, and suitable for work requiring x-ray examination.

CRACK-FREE BUTT WELDS IN HIGH-STRENGTH METALS

Formed structures of such hard-to-weld, but industrially important, materials as titanium, "superalloys," aluminum or air-hardening steels can now be fabricated readily with complete penetration, controlled contour welds—free of a major cause of failure in welded-joints, root-bead cracking. Such is the claim made by the M. W. Kellogg Co., Jersey City, N. J., in describing their new process of welding high-strength metals.

Reducing the human element is a considerable advance in eliminating the critical problem of lack of penetration. Today's growing application of inert-gas shielding—to the root bead's interior surface—has been an important step toward assuring complete penetration and producing completed welds of high quality. However, attaining full penetration in single-V butt welds, from one side without backing rings, has depended upon outstanding operator skill.

Where penetration is incomplete, there is also the unfortunate possibility that it may not be detected by available nondestructive examination. In ferritic materials, magnetic powder examination is also limited. Lack of penetration, even of minute dimension, interrupts an inside contoured surface with sharp notches.

With the newly developed process, these disadvantages are reportedly offset, since the control of penetration is inherent in the process. Through precise control of technique, physical dimension and internal gas shielding pressure, the process produces root beads of assured full penetration and controlled internal

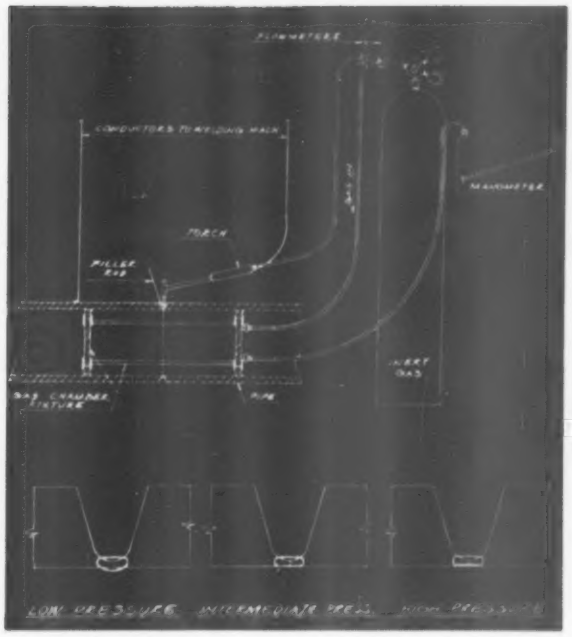


Fig. 1 *(Top)* Details of portable gas-chamber fixture. *(Bottom)* Shape of interior of weld surface is controlled by varying pressure

contour, a uniform root bead surface free of undercutting, and a high degree of root bead reinforcement—without backing rings and with only average operator proficiency.

In this process (see Figs. 1 and 2), inner and outer inert-gas shielded-arc welding is used in applying the initial or root bead to the joint being made up. By accurately controlling gas pressure inside pipe or weldment, a weld is deposited in this process with a highly uniform internal and external contour (see Figs. 3 and 4). By varying gas pressure, the inner surface of the bead can easily be made slightly convex, flush, or even concave—although the latter would rarely be used since it results in reduced weld strength.

Fig. 2 Root bead is laid while the pipe interior is under controlled inert-gas pressure in the gas-chamber fixture

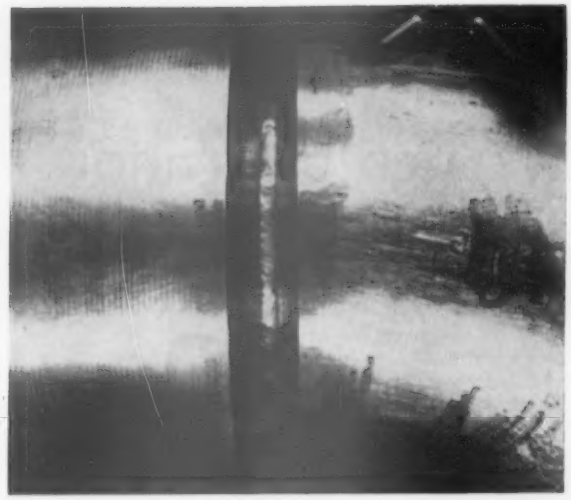


Fig. 3 Exterior view of the finished root bead which completes the first critical step in forming the weld

The weld quality obtained by this process is said to meet all X-ray, physical and mechanical requirements of the ASME Boiler Code, and other codes and specifications. Tensile, bend, impact and torsion tests have demonstrated excellent properties. Complete metallography has revealed that there is no tendency for cracking or undercutting the edges of the inside bead.

Although first used commercially in the shop fabrication and field installation of high-pressure, high-temperature stainless steel power piping, the process is being applied successfully to many other difficult welding applications. It is applicable to any metal or alloy which can be fusion welded. Welds produced by this process in heat-treatable steels such as SAE 4130 have withstood, without cracking, the drastic quench necessary to produce yield strengths in excess of 150,000 psi. Other materials welded successfully by this process are carbon and carbon molybdenum steels, chromium molybdenum steels, nickel, Monel, Inconel and copper.

Fig. 4 This is the view of the weld that the inspector gets. Note the highly uniform surface, entirely crack-free

WELDED CAST IRON PARTS HOLD HI-SPEED CENTRIFUGAL MOLD

BY H. C. WAUGH AND R. FRANK

Three years ago both the cast-iron rocker arm and support on a large foundry centrifugal casting machine suddenly sheared off simultaneously shortly after a casting had been poured. The flask or mold containing the molten metal tore loose and started a few small fires that were quickly extinguished. When the damage was cleaned up and the machine inspected, it looked rather hopeless. The rocker arm, a six by eight-inch piece of cast iron, was broken just above its pivot, and the top section of the upright support it locks into was cracked off. These two pieces make up part of the retaining mechanism that holds the flask in place as it spins at speeds up to 1200 rpm.

A centrifugal casting machine consists of a cylindrical flask or mold that lays horizontally on a bed of driver-rollers. In operation, the rollers spin, turning the flask at high speeds. Metal is poured into the mouth of the flask and centrifugal force shapes the molten metal inside the flask.

When the machine broke it had been casting shapes weighing from 250 to 1700 lb. The company was in the middle of a big contract and couldn't wait the six weeks it would take to get new parts.

After considering all the factors, it was decided that a nickel-iron electrode (55% nickel, 45% iron) be used to do the cast-iron welding job.

H. C. Waugh and R. Frank are associated with The International Nickel Co., Inc., New York, N. Y.

Preparation of the parts for welding was the first operation in the repair. All dirt and grime was removed from the weld area and the fractured surfaces were ground to provide a vee groove having an included angle of about 80°. The casting skin was ground back from the edge of the groove for approximately $^1/_2$-in The parts were then given a degreasing treatment.

Following these preparatory steps, the parts were properly aligned and preheated to 250° F using two oxy-acetylene torches. When the preheat temperature was reached, the parts were tack welded to hold them in line and welding began. Following deposition of each bead, the slag was removed, the bead wire brushed, and the area peened with a round nose tool and moderate blows. Using $^5/_{32}$-in. diam. nickel-iron electrodes the welding current was about 135 amp.

When both arm and support were finished, two one-in. by $^1/_4$-in. steel bands were placed over the top edges of the upright support and welded into place for additional strength. After the welds had cooled slowly under an asbestos blanket, they were ground flush, making them almost invisible against the base metal.

The company estimates that a new arm and support would have cost $500 and would have caused a delay of at least six weeks. Instead, it took about eight hr of welding and 20 lb of $^5/_{32}$-in. rod to put the machine back in operation. After three years, the centrifugal is still in regular use.

Fig. 1 Chalk stripes show where rocker arm on centrifugal casting machine sheared off; weld is nearly invisible

Fig. 2 Support also broke, was welded; metal band was welded around upper end of the support for more strength

Society News

Fall Meeting to Hear LaMotte Grover Deliver Adams Lecture

Plans have now been completed for a busy and informative National Fall Meeting to be held October 17–21 in Philadelphia.

Highlight of the meeting will be the Adams Lecture to be presented by LaMotte Grover of the Air Reduction Sales Co. The paper, "An Interpretation of Research and Experience in Structural Welding," will be delivered during the Monday morning Honors Session.

The technical sessions will feature many papers reporting on latest trends, developments and uses of welding. Over 60 papers will be delivered in the 21 sessions to be held at the Bellevue-Stratford, the headquarters hotel.

A few of the subjects to be discussed are carbon dioxide-shielded metal arc welding; advances in welding titanium and molybdenum; and application of welding to shipbuilding, piping, pressure vessels, machinery and many other fields.

Other papers will cover the welding of stainless steels, new approaches to welding with continuous-fed electrodes, pressure welding rails and how to use the relatively new metal powder electrodes. There will also be a session on brazing, resistance welding, inspection and testing, and weldability and research.

Other events to be held during the five-day meeting include the President's Reception and National Dinner, several tours through leading manufacturing plants in the Philadelphia area and educational lectures.

The AWS Fall Meeting will run concurrently with the National Metals Exposition at which welding and cutting exhibits and demonstrations will be shown. The Exposition will be held in Philadelphia's Convention Hall near the campus of the University of Pennsylvania.

THINGS YOU SHOULD KNOW

• In Kansas City, on June 8th, at the Society's Annual Business Meeting, the attending AWS members voted to amend the Society's Constitution as proposed in the Official Notice mailed to all AWS members on May 16, 1955. Fundamentally, the approved amendments now permit companies, firms, organizations, partnerships and associations, as well as individuals, to hold Sustaining Membership in our Society.

In addition the following new "Objects" were adopted:

(*a*) To encourage in the broadest and most liberal sense the advancement of welding;

(*b*) To encourage and to conduct research, both basic and applied, in all sciences as they relate to welding;

(*c*) To improve the education and usefulness of personnel engaged in and associated with welding activities;

(*d*) To engage in and assist others in the development of sound practices for the application of welding and related processes;

(*e*) To disseminate welding knowledge through its publications, meetings, discussions, consultations, exhibits and by any other available means;

Thereby fostering public welfare and education, aiding in the development of our country's industries and adding to the material prosperity and well-being of our people.

Later, at the meeting of the Board of Directors, appropriate changes were instituted in the By-Laws to accommodate the membership's vote on the constitution amendments.

• Upon recommendation of the Honorary Membership Committee, the Board of Directors voted unanimously to bestow Honorary Membership upon William Spraragen, Editor of The Welding Journal from 1920 to Jan. 1, 1955.

In addition, the following amendments to the By-Laws were voted upon favorably by the Board:

—that chairmen of AWS Standing Committees shall be corporate members of the Society

—that the Society's President shall be an *ex officio* member of all Standing and Special Committees, except the National Nominating Committee

—that in case of absence, disability or inaction of a committee chairman, the President may convene any committee, or it may be convened by the written request of any two of the members

—that Special Committees may be appointed by the President by and with the approval of the Board

—that Standing Committees shall submit annual reports and Special Committees interim and final reports to the Board of Directors.

All of the foregoing Board of Directors actions are to be submitted to the membership for approval by letter-ballot.

• The Committee on Code of Principles of Conduct submitted text for an AWS "Code of Ethics," which met with the Board's unanimous approval. Herewith follows the text:

"The American Welding Society expects that the following rules shall guide the acts of its members:

"1. To uphold before the public at all times, the dignity of membership in the American Welding Society and the reputation of the Society.

"2. To pursue his professional work in a spirit of fairness to employer, employee or customer with fidelity to contractor and to client, and to high ideals of personal honor.

"3. To avoid and discourage sensationalism, exaggeration and unwarranted statements.

"4. To uphold the principle that unusually low charges for professional work tend toward inferior and unreliable work.

"5. To refrain from undignified or misleading advertising. The use of the name or emblem of the AMERICAN WELDING SOCIETY in any advertising is not permitted.

"6. To treat as confidential his knowledge of the business affairs or technical processes of clients, employers or customers when their interests so require.

"7. To inform clients or employers of any business affiliations, interests or connections which might influence his judgment.

"8. To accept, for a particular service, financial or other compensation from one source only, except with the full knowledge and consent of all interested parties.

"9. To refrain from associating himself with, or allowing the use of his name by any enterprise of questionable character, or in any manner countenancing misrepresentation.

"10. To avoid injury, falsely or maliciously, directly or indirectly, to the professional reputation, prospects, or business of another member of the SOCIETY."

The foregoing "Code of Ethics" will be printed in the next re-issue of a "Year Book" Section. It will also be suitably and decoratively printed, in the manner of membership certificates, and made available to members who desire copies for mounting or framing.

• Final clarification was provided by the Committee on Awards concerning the procedure for the institution of Meritorious Certificate Awards of both National and District recognition. The Board approved the procedures which will be instituted Jan. 1, 1956.

•

Lew Gilbert, 1955–56 National Membership Chairman, is really going to work setting up an organization and formulating plans for all-out membership promotion during the coming year. Everyone will be hearing from him real soon. Let's all give him not only a hand but some real strong-arm and long-leg support. If we all sell AWS to our associates and friends, our SOCIETY will soon shoot up to the 20,000 member class!

•

The Niagara Frontier Section (Buffalo, to folks west of the Rocky Mountain Freight Zone and south of the Davy Crockett territory) elected Is Morrison to be chairman of the Niagara Frontier Arrangements Committee. This is the group that will provide the "wherehow" for putting on the 1956 Spring Technical Meeting. It is their responsibility to take care of all AWS members who are wise enough to come to Buffalo next May to the 4th National Spring Technical Meeting and Welding Show.

•

Even during these hotter than .... 1955 blister-year summer days (when this was written), we have been hearing welcome rumblings from a number of communities working up interest in starting new AWS Sections! It's a catching fever—and a swell one to catch! When a Section is successful in one city, it is not long before the next community asks itself—"Why can't we have one?" How about *your* town...would it not support a Section on its own; or possibly a Division of your nearest AWS Section? Your District Director will be mighty glad to give you a hand.

•

Summer (I repeat, it *was* summer when this was written) is generally the time to let up a bit and relax (...we certainly should have, after that Kansas City AWS Convention). But, that is not the way it is working out! On the contrary, everybody seems fully pepped up about going out this coming year and doing a bang-up job on programs and membership! Calls are coming in from everywhere for replenishment of Section supplies, literature, etc. It is certainly a healthy sign!

Take Lew Gilbert, for instance. He maintains a quota of four new ideas a day on promotion plans, and keeps his transcribing machine hot writing letters and inspirational messages. Is Morrison, who wears a number of AWS activity hats (even when driving around Buffalo in his souped-up factory-built "hot rod") is another fellow who never stops cooking up fresh ideas and brand new plans. And, lucky for us, there are many others in our midst who keep going at all times.

So, in the face of all this, we had no choice but to cut down on our Pumpkin Hollow vacation time a bit, get back here on the hot New York City asphalt, give Frank Mooney a chance to take a dip or two in the Atlantic Ocean, and whip yours truly into going to work again. And, now, here is our space end point and Bonney Rossi, our Editor, is standing over me with his August 1st deadline, and a "do you want to get that stuff in the September JOURNAL or not?"

And here all along I thought I was the boss!

JOE MAGRATH

An Open Letter to Members of AWS

Most of you—I hope—know me, or at least know of me. Many of you, undoubtedly, do not. In either case, however I want—most sincerely—to draw your attention to this letter, and to ask your cooperation.

Having recently been appointed National Chairman of the Membership Committee for the AMERICAN WELDING SOCIETY, and having been aware—for a long time—of the importance of increasing our SOCIETY'S membership, I am determined to try to do an all-out job in this respect. Since the best possible salesman for a good product is a consumer of that product, I have decided to place the major part of my emphasis toward this membership campaign in the laps of current members of this SOCIETY. It is for that reason I appeal to you for your help and support.

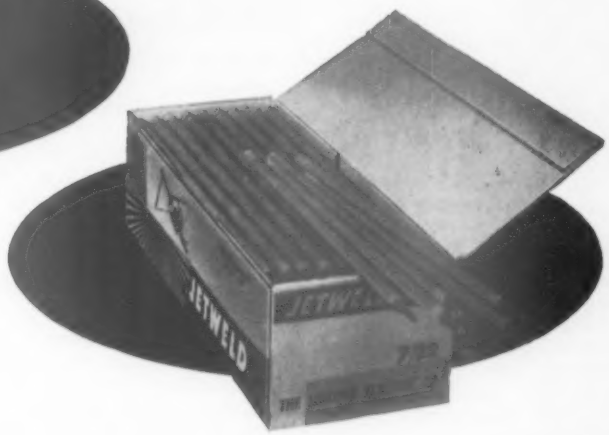
Just consider this fact. If each of you made a definite effort to bring in only *one* member this year, just one, the SOCIETY would double in membership! Think of that. Just the effort required to tell one good friend, or one business associate—or one of your suppliers—the advantages you gain from AWS membership could, this year, give us a membership of over 20,000 welding people. It's amazing that so little effort could accomplish so much.

Would you please do that for me? Will you promise yourself that you will discuss AWS membership with at least one of the people I mentioned? Will you make it your business to carry with you one or two applications for membership in your particular section? If you will do that, and will consider the advantages to yourself and to the entire industry which would result from that little bit of effort, you could help me to accomplish what I have set out to do. I want to double the membership of AWS this year. I sincerely—and humbly—appeal to you for your help, support and cooperation. May I have it? Your comments would be appreciated.

Cordially yours,
LEW GILBERT
Chairman, National Membership Committee
AMERICAN WELDING SOCIETY

If you do not have application blanks, we suggest you secure them through your AWS Section Secretary. If you are not in a Section then please write me and I will send them along immediately. —L. G.

Welding industry's greatest cost-cutting twins . . .



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Idealarc for easy, efficient operation . . . first welder to give you *both*
AC AND DC WELDING CURRENT
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TAC TALKS

FILLER METAL—I

Copper and Copper-Alloy Filler Metal

by F. E. Garriott

Two AWS-ASTM tentative specifications are available at nominal cost for your use; they are *Specifications for Copper and Copper-Alloy Welding Electrodes*, ASTM Designation B225, AWS Designation A5.6 and *Specifications for Copper and Copper-Alloy Welding Rods*, ASTM Designation B259, AWS Designation A5.6, issued in 1953 and 1952, respectively.

As indicated, the former applies to covered electrodes for shielded metal-arc welding and bare coiled wire for use with inert-gas consumable-electrode welding and submerged-arc welding. The latter applies to 36-in. welding rods for the oxy-acetylene, inert-gas nonconsumable electrode, atomic-hydrogen and carbon-arc processes.

Each specification covers in detail classification, chemical composition, mechanical requirements, testing methods, standard sizes, packaging, marking and guarantee. In addition they each contain appendices which serve as a guide in the use and application of these copper and copper-alloy electrodes and welding rods.

Most recent developments in the copper-alloy electrode field not covered by these specifications include stranded wires of various compositions for use with the consumable-electrode method, a covered nickel-aluminum bronze electrode and covered manganese bronze electrodes for metal-arc welding. New color code identification markings were assigned to bronze electrodes by NEMA effective November 1954 and are now being used by the industry.

In view of the new developments, it is contemplated that revision of these specifications will become necessary in 1956. Therefore, all suggestions and recommendations regarding future proposed changes in these specifications will be welcomed by Subcommittee VI of your Filler Metal Committee.

F. E. Garriott is Chairman, Subcommittee VI, AWS-ASTM Committee on Filler Metal; Manager, Weldrod and Wire Department, Ampco Metal, Inc.

Specifications for Stainless Steel Electrodes

by R. David Thomas, Jr.

Ever since completing the stainless welding rod and bare electrode specifications, Subcommittee IV of the AWS-ASTM Committee on Filler Metal has been reviewing drafts and studying revisions in the covered electrode specifications. The products of its efforts are available in the new edition of the stainless electrode specifications which were recently issued.

These specifications replace the previous edition which is now seven years old. The title is the same, except for the insertion of the word "covered" to clearly distinguish it from the bare electrode standard: *Tentative Specifications for Corrosion-Resisting Chromium and Chromium-Nickel Steel Covered Welding Electrodes.*

The new specifications differ from the earlier one primarily by the addition of eight new classifications to the ten classifications covered previously. The extra-low carbon stainless steels—Types 308 and 316—are two of the new classifications added; columbium and molybdenum modifications of Types 309 and 310 are also new in the 1955 edition; Types 312 (29 Cr and 12 Ni) and 318 (once called 316 Cb) are the other newcomers.

Another simplification has been accomplished by deleting the usability classifications −25 and −26. These were intended to cover those electrodes which were designed for downhand welding only. A survey showed that all electrodes qualified as all-position types in the sizes specified so that the only usability classifications required were −15 for d-c reverse polarity, all-position operation and −16 for a-c or d-c reverse polarity, all-position operation.

The companion stainless filler metal specification *Corrosion-Resisting Chromium and Chromium-Nickel Steel Welding Rods and Bare Electrodes* contains corresponding classifications for use with atomic-hydrogen, inert-gas metal-arc and submerged-arc welding.

R. David Thomas, Jr., is Chairman, Subcommittee IV, AWS-ASTM Committee on Filler Metal; Vice-President, Arcos Corp.

Aluminum and Aluminum-Alloy Filler Metal

by G. O. Hoglund

An up-to-date specification on filler metals for welding the aluminum alloys has been published as a joint effort of the AWS and ASTM. This is specification A5.10 titled *Aluminum and Aluminum-Alloy Welding Rods and Bare Electrodes.*

The specification is suitable for choosing filler metal to weld high purity aluminum, commercially pure aluminum, the aluminum-magnesium alloys, the heat-treated magnesium-silicon alloys and many of the commercial casting alloys. The specification shows in detailed form the composition of the filler metals.

The appendix contains useful information on procedure. More important, it provides recommendations which permit the choice of the correct filler metal for the various aluminum alloys. Both welding rods and electrodes are discussed from this standpoint.

This specification is perhaps the most up-to-date publication in this field at this time. It is used as the basis for choosing filler metal in the ASME Unfired Pressure Vessel Code. It will find the same function in the new API Storage Tank Code where aluminum alloys are concerned.

The specification represents a good deal of work on the part of AWS Subcommittee or Aluminum and Aluminum-Alloy Filler Metal.

G. O. Hoglund is Chairman, Subcommittee III, AWS-ASTM Committee on Filler Metal; Alcoa Process Development Labs., Aluminum Company of America.

Mark Your Calendar, Now for OCTOBER 17-21, 1955

AWS NATIONAL FALL MEETING

Bellevue-Stratford Hotel, Philadelphia, Pa.

Section News and Events

As reported to Catherine O'Leary

ANNUAL PICNIC

Dayton, Ohio—The final meeting of the 1954–55 season of the *Dayton* Section was the Annual Picnic held on June 14th at the Inland Activities Center. Approximately 130 members, wives and guests were present.

The picnic started at 5 P.M. with a variety of activities which kept everybody busy until dinner time.

After an excellent dinner, bingo was played with many valuable prizes being given to the winners. Clarence Smultheis, the picnic chairman, and his committee, are to be congratulated for the excellent job that they did on this picnic. The affair was thoroughly enjoyed by all, and it is hoped that it can always be as well planned.

METALLURGY

Oak Ridge, Tenn.—Sidney Low (AWS), of Chapman Valve Co., Indian Orchard, Mass., was the speaker at the April 18th meeting of the *Northeast Tennessee* Section. His subject was "Casting, Welding, and High-Temperature Metallurgy."

STRUCTURAL MATERIALS

Seattle, Wash.—An interesting discussion on the subject of "New Structural Materials and Their Influence on Welding Procedures," was given by LaMotte Grover (AWS), welding engineer, Air Reduction Co., New York, at the May 18th meeting of the *Puget Sound* Section. Mr. Grover pointed out the improved properties to be expected from these new materials and discussed the welding procedures and recommended techniques. Slides were shown to illustrate the testing procedures of various welding structures and some failures that have occurred.

STRUCTURAL WELDING

San Francisco, Calif.—The regular monthly dinner meeting of the *San Francisco* Section was held on May 23rd at the Engineers Club. Speaker at the meeting was LaMotte Grover (AWS), welding engineer, Air Reduction Co., New York, who discussed the new structural steel materials that are now available for the specification requirements for welding procedures; where the newer steel materials and electrodes can be used to good advantage, and where not.

This was a joint meeting with the Structural Engineers Association of Northern California. A total of 185 members of both associations turned out for the meeting.

PLANT TOUR

San Jose, Calif.—On May 24th, 250 members and guests of the *Santa Clara* Section took part in the conducted plant tour through the new Milpitas Ford plant.

ANNUAL PICNIC

Sheffield, Ala.—The Annual Picnic of the *Tri-Cities* Section was held on June 24th at the Reynolds Camp on Wilson Lake shores. Dancing, swimming, boating and games were enjoyed by the 50 members who attended.

Authors Invited!

Invitations to prospective authors for the 1956 AWS National Spring Meeting in Buffalo have recently been issued by National Secretary J. G. Magrath. Those who plan to take advantage of this new opportunity are urged to send in their filled-in Abstract Form at the earliest possible date. To assure full consideration, abstracts must reach AWS headquarters not later than October 15, 1955.

SECTION MEETING CALENDAR

SEPTEMBER 16th

TRI-CITIES Section, Sheffield, Ala., T.V.A. Auditorium. "Stresses, Concentration, and Welding Design Using a Polarized Light Demonstration," by John Durstine, Lincoln Electric Co., Birmingham, Ala.

SEPTEMBER 19th

NORTHEAST TENNESSEE Section, Oak Ridge Knights of Columbus Hall, 8:00 P.M.

SEPTEMBER 26th

CORPUS CHRISTI, TEX. Engineers Club, 1552 South Staples Street. "Resistance Welding," by Frank W. McBee, Jr., Defense Research Lab., University of Texas.

SEPTEMBER 27th

SAN ANTONIO, TEX. Family Social Gathering.

SEPTEMBER 28th

HOUSTON, TEX. "Evaluation of Iron Powdered Electrodes for Production Use," by D. B. Howard, Superintendent of Metallurgical Research, General Laboratories, American Car and Foundry Co., Berwick, Pa.

SEPTEMBER 29th

BEAUMONT, TEX. (See September 28th for subject and speaker.)

SEPTEMBER 30th

AUSTIN, TEX. (See September 28th for subject and speaker.)

OCTOBER 3rd

LEHIGH VALLEY Section. Walp's Restaurant, Allentown, Pa. Dinner at 6:30 P.M. Meeting at 8 P.M. "Better Welding at Lower Cost," by Lew Gilbert, Executive Editor, *Industry and Welding*.

OCTOBER 7th

NORTH CENTRAL OHIO Section. Dinner at 6:30 in the Mansfield Leland Park Avenue Room, Mansfield, Ohio; followed at 8:00 P.M. by plant tour through Empire Steel Corporation.

OCTOBER 11th

DAYTON Section. Dayton Engineers Club. "Automation," by Robert H. Blair, Resistance Welder Corp.

OCTOBER 13th

IOWA ILLIONOIS Section, Moline, Ill., 7:30 P.M. American Legion Club. "Application of Resistance Welding," by Karl Matthes, Precision Welder and Flexopress Corp.

Q. Is it possible to be certain we are using the right filler metal for consumable electrode welding?

A. Yes. Alcoa's new I.G.* Electrode is available in such a wide range of alloys and sizes that there is a right one for every job.

Every production lot of Alcoa* I.G. Electrode is test welded and the welds X-rayed for soundness. Each 10-lb spool is individually packed to maintain the super-smooth finish to assure soundness of weld and smoothness of feed. Each of five alloys, 1100, 4043, 5154, 5254 and 5356 (formerly 2S, 43S, A54S, B54S and XC56S), is available in six diameters. There is no better electrode.

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Your guide to aluminum value

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Gentlemen: Please send book, *Welding Alcoa Aluminum*.

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Title_____

Address_____ City_____ State_____

Q. Where is Alcoa's new I.G. Electrode available?

A. Contact your nearest Alcoa sales office, or, for immediate delivery, call one of the Alcoa Distributors listed below. He carries a complete range of alloys and sizes, and you'll find him well qualified to work with your welding experts.

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Birmingham
†Hinkle Supply Co.
CALIFORNIA
Berkeley
Ducommun Metals & Supply Co.
Los Angeles
†Ducommun Metals & Supply Co.
Pacific Metals Company, Ltd.
San Diego
Ducommun Metals & Supply Co.
San Francisco
†Pacific Metals Company, Ltd.
COLORADO
Denver
Marsh Steel Corp.
Metal Goods Corp.
CONNECTICUT
Milford
†Edgcomb Steel of New England, Inc.
FLORIDA
Jacksonville
Florida Metals, Inc.
Miami
Florida Metals, Inc.
Tampa
†Florida Metals, Inc.
GEORGIA
Atlanta
†J. M. Tull Metal & Supply Co., Inc.
IDAHO
Boise
Pacific Metal Co.
ILLINOIS
Chicago
†Central Steel & Wire Company
Corey Steel Co.
†Steel Sales Corp.
INDIANA
Indianapolis
Steel Sales Co. of Indiana, Inc.
KANSAS
Wichita
Marsh Steel Corp.
Metal Goods Corp.
KENTUCKY
Louisville
Williams & Co., Inc.
LOUISIANA
New Orleans
Metal Goods Corp.
MARYLAND
Baltimore
Whitehead Metal Products Co., Inc.
MASSACHUSETTS
Cambridge
Whitehead Metal Products Co., Inc.
MICHIGAN
Detroit
Central Steel & Wire Company
Steel Sales Co. of Michigan
MINNESOTA
Minneapolis
Steel Sales Co. of Minnesota
MISSOURI
Kansas City, North
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Metal Goods Corp.
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NEW HAMPSHIRE
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Williams & Co., Inc.
Cleveland
†The Hamilton Steel Warehouse
Jones & Laughlin Steel Corp.
Williams & Co., Inc.
Columbus
Williams & Co., Inc.
Toledo
Williams & Co., Inc.
OKLAHOMA
Tulsa
Metal Goods Corp.
OREGON
Portland
†Pacific Metal Co.
PENNSYLVANIA
Philadelphia
†Edgcomb Steel Co.
Whitehead Metal Products Co., Inc.
Pittsburgh
†Williams & Co., Inc.
York
Edgcomb Steel Co.
TENNESSEE
Memphis
Metal Goods Corp.
TEXAS
Dallas
Metal Goods Corp.
Houston
Metal Goods Corp.
UTAH
Salt Lake City
Pacific Metals Company, Ltd.
WASHINGTON
Seattle
Pacific Metal Co.
WISCONSIN
Milwaukee
Central Steel & Wire Company
Steel Sales Co. of Wisconsin

†Home Office

New Section Officers for 1955–56 Fiscal Year

Anthony Wayne—Chairman, D. B. Rice; 1st Vice-Chairman, Jacob S. Geiser; 2nd Vice-Chairman, Delbert Clark; Secretary-Treasurer, Carl Cary; Chr. Membership Committee, Ernest Albert; Chr. Program Committee, Delbert Clark; Technical Representative, Kenneth Zimmerman

Birmingham—Chairman, O. G. Thomas; 1st Vice-Chairman, R. E. Clotfelter; Secretary-Treasurer, K. V. Nickel

Cleveland—Chairman, Allan P. Stern; 1st Vice-Chairman, Arthur G. Portz; 2nd Vice-Chairman, William P. Mayor; Secretary-Treasurer, Robert J. Kriz; Chr. Program Committee, Arthur G. Portz; Chr. Symposium Program Committee, Wm. P. Mayor

Colorado—Chairman, C. Brinton Swift; 1st Vice-Chairman, Edw. O. Browning; 2nd Vice-Chairman, C. E. Withers; Secretary-Treasurer, C. E. Reissig; Chr. Membership Committee, Herman A. Geller; Chr. Program Committee, David C. Card; Technical Representative, Robert B. Craig, Jr.

Dayton—Chairman, Robert M. Hous; 1st Vice-Chairman, William H. Hobart, Jr.; Secretary, Charles O. Adams; Treasurer, Clarence Shultheis; Chr. Membership Committee, B. K. Feinthal; Chr. Program Committee, William H. Hobart, Jr.; Technical Representative, Albert J. Mealey

Fox Valley—Chairman, A. C. Mulder; 1st Vice-Chairman, H. Skatrud; 2nd Vice-Chairman, J. Revelt; Secretary, C. G. Malmberg; Treasurer, John Wiegand; Chr. Membership Committee, G. K. Willecke; Chr. Program Committee, H. Skatrud; Technical Representative, C. J. Sappington

Holston Valley—Chairman, Robert Lahr; Secretary-Treasurer, R. T. Rogers; Chr. Membership Committee, Glenn Clore

Kansas City—Chairman, Dean E. Broderson; 1st Vice-Chairman, Frank G. Singleton; Secretary, Blaine G. Harrington, Jr.; Treasurer, Tom O. McDowell; Chr. Membership Committee, Frank G. Singleton; Chr. Program Committee, William J. Newton; Technical Representative, Leslie N. Williams

Louisville—Chairman, Lawrence N. Boosey; 1st Vice-Chairman, Richard E. Odell; Secretary, William C. Davis; Treasurer, Chester A. Hare; Chr. Membership Committee, Herbert L. Martin; Chr. Program Committee, Richard E. Odell; Technical Representative, Arthur B. Doudna

Madison—Chairman, Howard Adkins; 1st Vice-Chairman, Fred E. Theiler; 2nd Vice-Chairman, Arnold Ehle; Secretary - Treasurer, Raymond Knudtson; Chr. Membership Committee, George Sell; Chr. Program Committee, Fred E. Theiler; Technical Representative, Russell J. Herreid

Mahoning Valley—Chairman, W. F. Buchanan; 1st Vice-Chairman, J. Manternach, Jr.; Secretary-Treasurer, N. G. U'Halie; Technical Representative, Dr. I. A. Oehler

Maryland—Chairman, A. Clemens, Jr.; 1st Vice-Chairman, R. L. Sibley; 2nd Vice-Chairman, C. Basel; Secretary, J. H. Burnley; Treasurer, B. F. Jones; Chr. Membership Committee, C. Basel; Chr. Program Committee, R. L. Sibley; Technical Representative, R. E. Metius

Milwaukee—Chairman, Joseph H. Stevens; 1st Vice-Chairman, Francis J. Harencki; Secretary-Treasurer, Anton L. Schaeffler; Chr. Membership Committee, Louis L. Ahsinger; Chr. Program Committee, Francis J. Harencki; Technical Representative, Robert J. Keller

North Central Ohio—Chairman, Robert A. Ellison; 1st Vice-Chairman, Solon McDonald; 2nd Vice-Chairman, Ray B. Leiter; Secretary, Howard B. Cary; Treasurer, Joseph D. Henne

Northeast Tenn.—Chairman, Gerald M. Slaughter; 1st Vice-Chairman, J. C. Thompson; 2nd Vice-Chairman, Glenn E. Elder; Secretary, A. A. Walls; Treasurer, E. N. Rogers; Chr. Membership Committee, L. R. Bledsoe; Chr. Program Committee, J. C. Thompson; Technical Representative, E. C. Miller

Oklahoma City—Chairman, Ed. Lindsey; 1st Vice-Chairman, C. W. Case; Secretary, Robert A. Bernier; Treasurer, Lon Erickson; Technical Representative, Paul Wedel

Olean-Bradford—Chairman, Michael H. Robinson; 1st Vice-Chairman, Albert W. Langley; Secretary, Andrew P. Kasper; Treasurer, Sid L. Worth; Technical Representative, Andrew P. Kasper

Puget Sound—Chairman, Roy A. Kamb; 1st Vice-Chairman, Eric Kinnaird; Secretary-Treasurer, D. E. Orth

Rochester—Chairman, Nelson Martin; 1st Vice-Chairman, Robert Barr; Secretary, Walter Dick; Treasurer, Robert Cattanach; Chr. Membership Committee, George Davies; Chr. Program Committee, Robert Barr; Technical Representative, Walter Dick

Santa Clara—Chairman, Wallace J. Erichsen; 1st Vice-Chairman, Fortune Masdeo; 2nd Vice-Chairman, Neil Shuirman; Secretary, Jack W.

NEW G-E Automatic Welding System Uses CO_2 to Slash Gas Costs 95%

FILLERARC . . . first *complete* equipment designed especially for automatic gas-shielded welding; combines unique generator with low-cost carbon dioxide—for sound welds on mild steel.

General Electric's new Fillerarc Automatic Welding Equipment brings you—not only 95% savings in shielding-gas costs—but ***sound welds on mild steel.*** The great savings possible through use of CO_2 as a shielding agent have long been recognized—but good mild steel weld quality has never been obtained. Now, after two years of successful application of Fillerarc equipment on non-ferrous metals (using inert gas), G-E engineers bring you the total solution for mild steel welding—***savings plus high quality.***

95% SAVINGS ARE POSSIBLE because CO_2 costs only ***1/20th*** as much as inert gas. For the average job, Argon would cost $5.00 per hour, CO_2 only twenty-five cents. This means yearly savings per equipment may be as high as $20,000 on a 3-shift, 60% duty cycle basis. This new G-E system can now replace the submerged arc method on many jobs and eliminate many of the problems associated with granular flux.

EXCLUSIVE G-E GENERATOR practically eliminates the erratic weld quality formerly obtained with CO_2. Here's why: Only the G-E Fillerarc generator—with its exclusive ***rising*** volt-ampere characteristic—supplies an arc length which always remains constant, even with adjustments in wire speeds. This means minimum weld porosity and spatter—and deep penetration for extra-strong welds.

COMPARED TO OTHER INERT-GAS SYSTEMS, G-E Fillerarc equipment . . .

- Reduces gas costs 95%, for yearly savings up to $20,000 per equipment;
- Delivers consistent weld quality because of the unique G-E Fillerarc generator;
- Assures constant speed; system is insensitive to normal line-voltage changes;
- Provides wire-feed speeds as high as 1000 I.P.M.

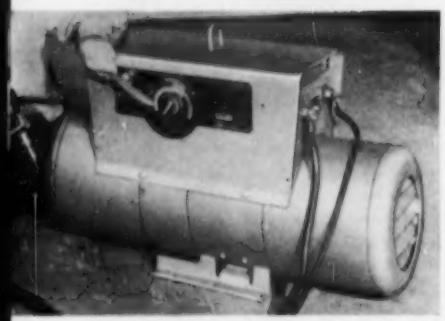
COMPARED TO FLUX-SHIELDED METHODS, G-E automatic Fillerarc equipment . . .

- Deepens penetration with low-cost CO_2;
- Provides a visible arc for full control of weld quality, permits less expensive fixtures;
- Does not use abrasive flux—especially important in welding compressor housings and similar jobs;
- Eliminates stubborn flux slag; cleaning costs are greatly reduced;
- Eliminates expensive flux recovery systems.

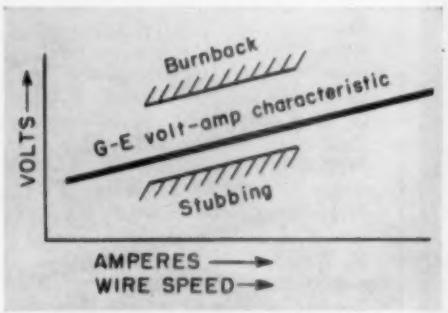
FOR MORE DATA contact your G-E welding sales engineer—and write for bulletin GEC-1334 to Section 713-1, General Electric Co., Schenectady, N. Y.

SEE G-E automatic Fillerarc equipment demonstrated at the National Metal Exposition, Philadelphia, October 17–21.

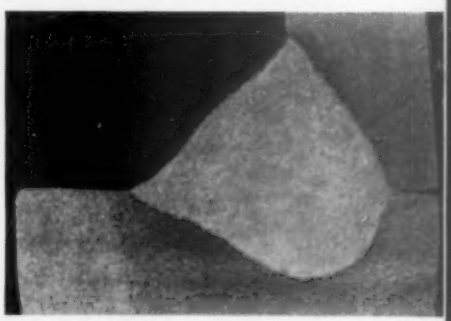
Why the complete G-E system makes possible quality mild-steel welds with CO_2



G-E SELF-REGULATING GENERATOR is the heart of the Fillerarc Automatic Welding system. Unique rising volt-amp curve maintains constant arc length.



RISING VOLT-AMP CURVE of the exclusive G-E Fillerarc generator matches rising arc curves . . . does not get into area of burnback and stubbing. Resulting high weld quality is uniform.



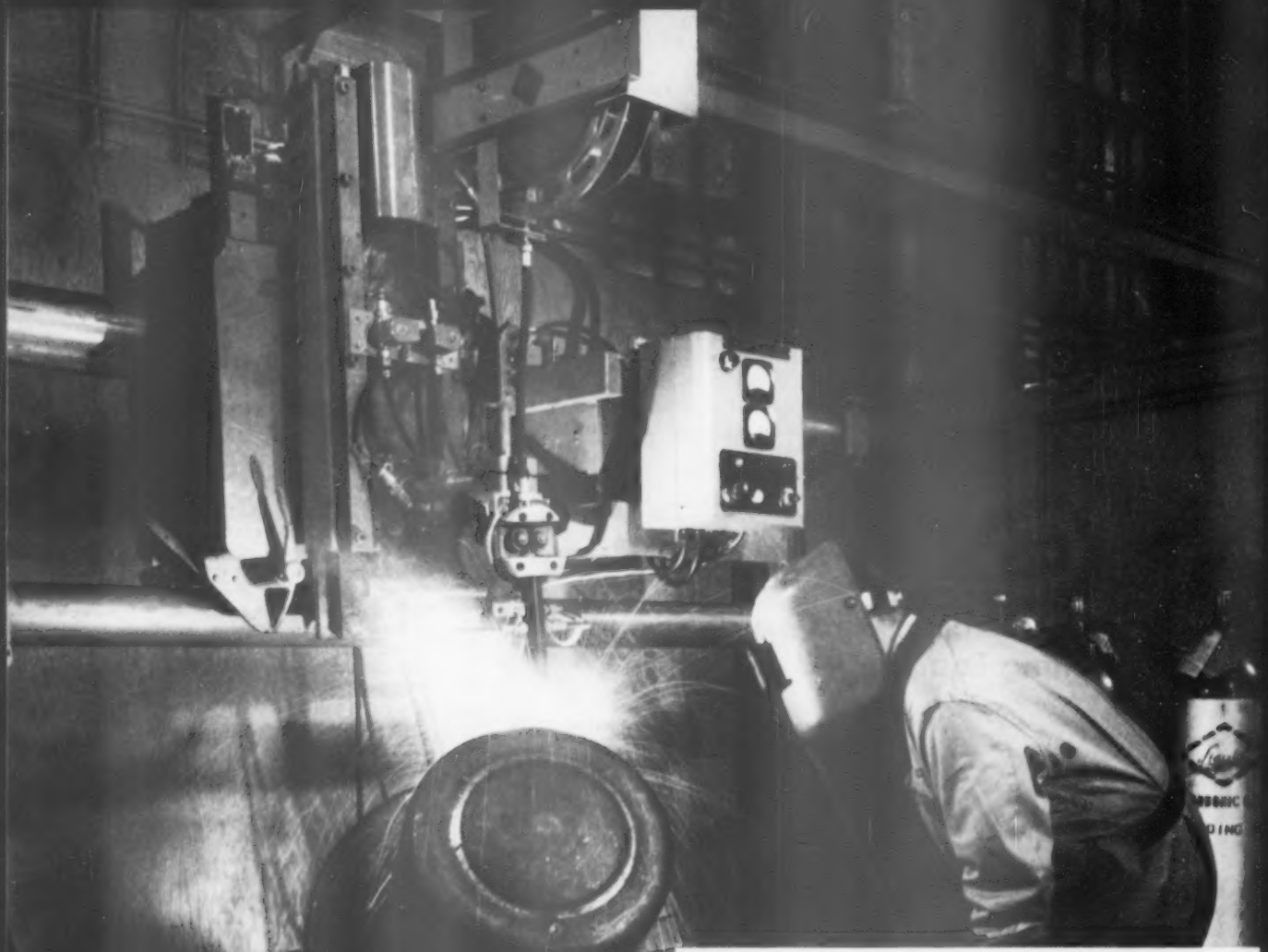
DEEP PENETRATION is produced by short arcs and faster wire speeds tpyical with Fillerarc equipment. Above: weld on semi-killed steel.

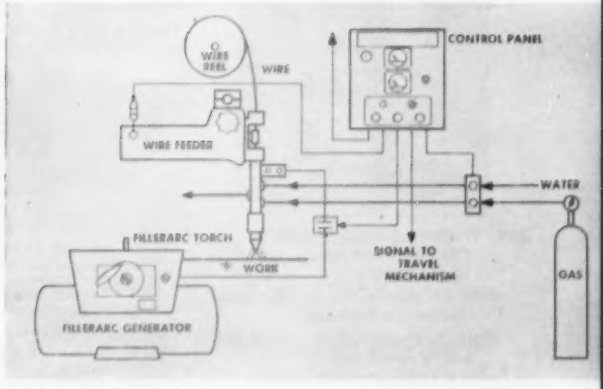
Progress Is Our Most Important Product

GENERAL ELECTRIC

†Home Office

WELDING ARCS NEWS





G-E FILLERARC EQUIPMENT is the only complete equipment especially designed for consumable-electrode gas-shielded welding. Simplicity of the system makes possible use of less expensive fixtures and greater over-all efficiency.

Garrels; Treasurer, A. M. Hubbard; Chr. Membership Committee, Richard L. Belding; Chr. Program Committee, Fortune Masdeo; Technical Representative, Ronald N. Skow

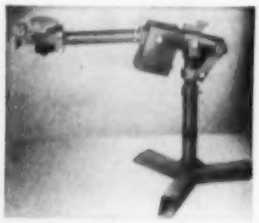
Syracuse—Chairman, Edward Strand; 1st Vice-Chairman, Arthur Cottet; Secretary, Robert M. Larkin; Treasurer, Lowell Hawthorn; Chr. Membership Committee, Arthur Cottet; Chr. Program Committee, Arthur Cottet; Technical Representative, Harry Miller

Toledo—Chairman, L. R. Shanteau; 1st Vice-Chairman, W. F. Schmidt; 2nd Vice-Chairman, R. E. Linker; Secretary, M. W. Eddins; Treasurer, S. W. Snell; Chr. Membership Committee, R. E. Linker; Chr. Program Committee, W. F. Schmidt; Technical Representative, M. J. Baughman

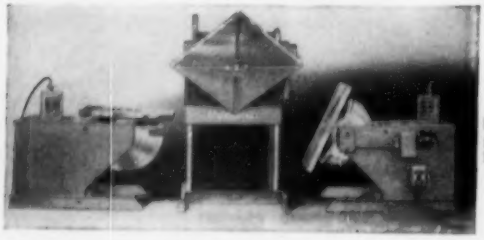
Western Michigan—Chairman, Douglas J. Harvey; 1st Vice-Chairman, Ernst Duesing; 2nd Vice-Chairman, Eric Guenther; Secretary, Gordon L. Hill; Treasurer, Chester H. Voorhorst

Wichita—Chairman, Roy Whitt; 1st Vice-Chairman, Glenn Woodmancy; 2nd Vice-Chairman, Gene Scott; Secretary, H. E. Nelson; Treasurer, R. M. Prescott; Chr. Membership Committee, Howard Stauffer; Chr. Program Committee, Glenn Woodmancy; Technical Representative, Paul Carter

Steady, Positive, Precision Rotation for AUTOMATIC WELDING with *Aronson* Positioning Equipment.

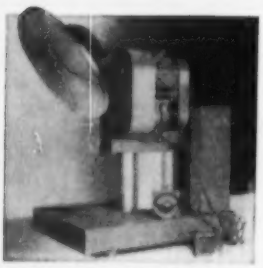


UNIVERSAL Balanced POSITIONING
Model C 500 shown
25 lbs. to 2,000 lbs. capacity



HEAVY DUTY GEAR DRIVEN POSITIONING
Model HD 25 shown
500 lbs. to 24,000 lbs. capacity

HEAD and TAIL STOCK POSITIONING
Model HTS 5 shown
Capacities 5,000 lbs. to 160,000 lbs.



MODEL 21 (shown) SUPER GEAR DRIVEN POSITIONER with powered rotation tilt and elevation.

MODEL X-21 Powered rotation, ball bearing manual tilt and elevation.

MODEL 21-P Powered rotation and tilt, ball bearing manual elevation.

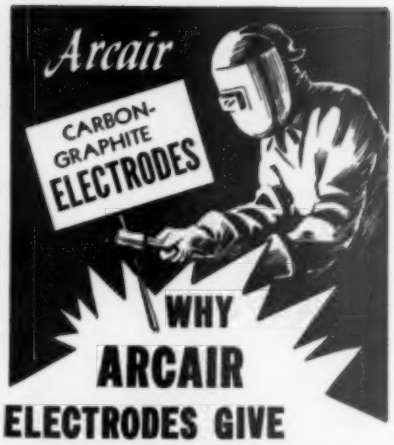
MODEL 21-E Powered rotation and elevation, ball bearing manual tilt.

MODEL X-21-PE Powered rotation, tilt and elevation. 5000 lbs. capacity @ 6" CG location.

Aronson TRACTRED Turning Rolls
Steady, positive, precision rotation for automatic welding. MODEL T1 (shown) capacity 6,000 lbs. per unit. MODEL T2 capacity 12,000 lbs. per unit. MODEL T3 capacity 18,000 lbs. per unit.

HEAVY-DUTY PRECISION BUILT RUBBER TIRED TURNING ROLLS
5,000 lbs. to 150 tons capacity

Aronson MACHINE COMPANY
ARCADE, NEW YORK



ELECTRODES GIVE BETTER RESULTS AND SAVE YOU MONEY!

Arcair electrodes enable you to realize the full performance and economy designed into your Arcair torch. No substitute electrode will perform as well or give as much value as genuine Arcair carbon-graphite electrodes. Arcair electrodes employ a special compound of carbon and graphite, developed and processed especially for use with the Arcair torch. This unique blending of carbon and graphite results in an electrode with the best qualities of each material, as well as superior arc stability. Recent tests proved the average life of an Arcair electrode to be *36% better than a popular competitive grade, yet Arcair costs about one half as much!*

Arcair superior carbon-graphite electrodes are available in plain or copper-coated types in sizes 5/32" to 1/2" in diameter. Special metal electrodes are also available for exceptional requirements of cast iron and copper alloy work. We want you to prove the money saving features of these electrodes ON YOUR OWN WORK! Write us for FREE SAMPLES of Arcair electrodes, try them at no charge, and you will quickly see why YOU CAN'T AFFORD NOT TO USE THEM! With your FREE ELECTRODES, we'll send complete details of these finer electrodes, along with additional test data.

WRITE FOR YOURS TODAY!

ARCAIR COMPANY
431 South Mt.
Pleasant Ave.
Lancaster, Ohio

Arcair TORCH
Cuts all metals—using only electric arc and compressed air

New Members

Effective July 1, 1955

MEMBERSHIP CLASSIFICATION

A—Sustaining Member B—Member C—Associate Member D—Student E—Honorary Member F—Life Member

BIRMINGHAM

Eades, Joseph W. (B)
Hill, B. Taylor (B)

BOSTON

Murphy, Richard Lawrence (B)

BRIDGEPORT

Crossland, Alfred C. (B)
Grzesczak, Charles A. (B)

CHICAGO

Golon, Edward (C)
Matusek, Joe J. (B)
Pawlowski, Henry (B)

CINCINNATI

Nethero, Merle F. (B)

CLEVELAND

Cholfin, Robert B. (C)
Elsman, Joseph M. (C)
Jacobs, Howland D. (B)
La Vine, Leroy C. (C)
Moreau, Joseph A. (C)
Thurston, Thruman G. (C)

COLUMBUS

Swanson, John T. (C)

DALLAS

Stewart, Dean A. (C)
Twohey, W. E. (C)

DAYTON

Frierwood, Eugene L. (C)
Gue, Frank E. (C)
Heidt, Milton R. (C)
Hoover, Clarence W. (B)

DETROIT

Goodman, Gilbert (C)
Laurie, Dorthy (B)
O'Fallon, John E. (C)
Stewart, William Wallace (C)

EAST TEXAS

York, Jack L. (B)

FOX VALLEY

Stolzenberg, Fred O. (C)

HOUSTON

Barrett, David S. (C)
Foster, Aubrey G. (B)
Garrison, Marion D. (B)
Kanipe, C. Keith (B)
Taylor, R. B. (C)
Tolin, Richard B. (B)
Warriner, Fred A. (B)

IOWA

Kelleher, Patrick (C)

KANSAS CITY

Bullard, Donald E. (B)
Humbert, Joe (B)
Johnson, Acie H., Jr. (C)
Marks, W. F. (B)
Pierce, Leroy P. (B)
Zorn, William W. (C)

LOS ANGELES

Alewel, Leslie H. (C)
Baird, Bruce L. (C)
Cook, Edmond F. (C)
Fulbright, Donald (C)
Lester, W. J. (C)
Sysum, Robert L. (B)
Yapelli, Frederick A. (C)
Zumwalt, K. E. (C)

MARYLAND

Nelson, James O. (C)
Rienhoff, Hugh Young (B)

MILWAUKEE

Crowley, L. G. (C)
Steidl, Ervin A. (C)

NASHVILLE

Wilson, A. R., Jr (C)

NEW JERSEY

Holster, Ira D., Jr. (B)
Skerchek, Michael A. (B)
Staton, Lee L. (B)

NEW ORLEANS

Bauer, Emil R., Jr. (B)
Bougere, Fernand F. (B)
Bruno, S. A. (C)
Lockwood, Herman L. (C)
Stewart, George S. (B)
Yates, Harry L. (B)

NEW YORK

Logan, Robert, Jr. (C)
Roddy, Charles (C)
Sutton, Roger (B)

NIAGARA FRONTIER

Griffiths, Donald A. (C)
Meigs, Perry L. (C)

NORTH CENTRAL OHIO

Milum, John P. (C)

NORTHERN NEW YORK

Ryan, Richard (B)
Tuttle, Frank B. (C)

NORTHWEST

Hewitt, R. H. (C)
Quigley, Kenneth L. (B)

OKLAHOMA CITY

Adams, D. E. (B)
Prowell, James (B)

PHILADELPHIA

Ascenzi, C. (B)
Barr, Charles A. (B)
Brown, J. R., Jr. (B)
Chopko, S. A. (B)
Dixon, J. S. (B)
Graff, F. J. (B)
Greenfield, E. (B)
Hager, J. R., Jr. (B)
Holmstock, Jacob C. (B)
Hurm, D. C. (B)
McDevitt, J. (B)
Mihm, William J. (B)
Miller, R. A. (B)
Morgan, W. R. (B)
Prete, D. P. (B)
Rizzo, F. S. (B)
Thomas, W. B. (B)
Whitehair, B. M. (B)

PITTSBURGH

Black, William H. (B)
Gent, George R. (C)

PUGET SOUND

Poloson, Fred Harris (B)

RICHMOND

Nunnally, Elliot D. (B)

SAGINAW VALLEY

Kuntz, Glenn, C. (D)

ST. LOUIS

Harris, William M. (B)

SANTA CLARA VALLEY

Develine, Robert H. (B)

SOUTH FLORIDA

Boronski, James N. (B)
Jureit, John Calvin (B)

SUSQUEHANNA VALLEY

Barrows, Edward S. (C)

SYRACUSE

Brenan, William (C)
Loew, Charles E. (C)
Masek, Benjamin (C)
Saul, Francis W. (C)

TOLEDO

Lorenzen, Thomas L. (C)

TRI-CITIES

Yoder, Roy L. (B)

TULSA

Fleming, Edwin H. (B)

WESTERN MASS.

Jaeger, Leon W., Jr. (C)
Morse, John W. (B)

WESTERN MICH.

Dauser, Robert O. (C)
Gill, Harry M. (C)
Henson, Lyn (B)
Masek, William A. (C)
Paul, Claude L. (C)
Salatka, George A. (C)

NOT IN SECTIONS

Eder, James (C)
Jones, Burrell P. (C)
Kitano, Hiroo (B)
Padam, S. K. (C)
Shane James H. (C)
Siegfried, W. (B)
Ujiie, Akira (B)

Members Reclassified During the Month of July

HOUSTON

Allen, Marvin K. (C to B)

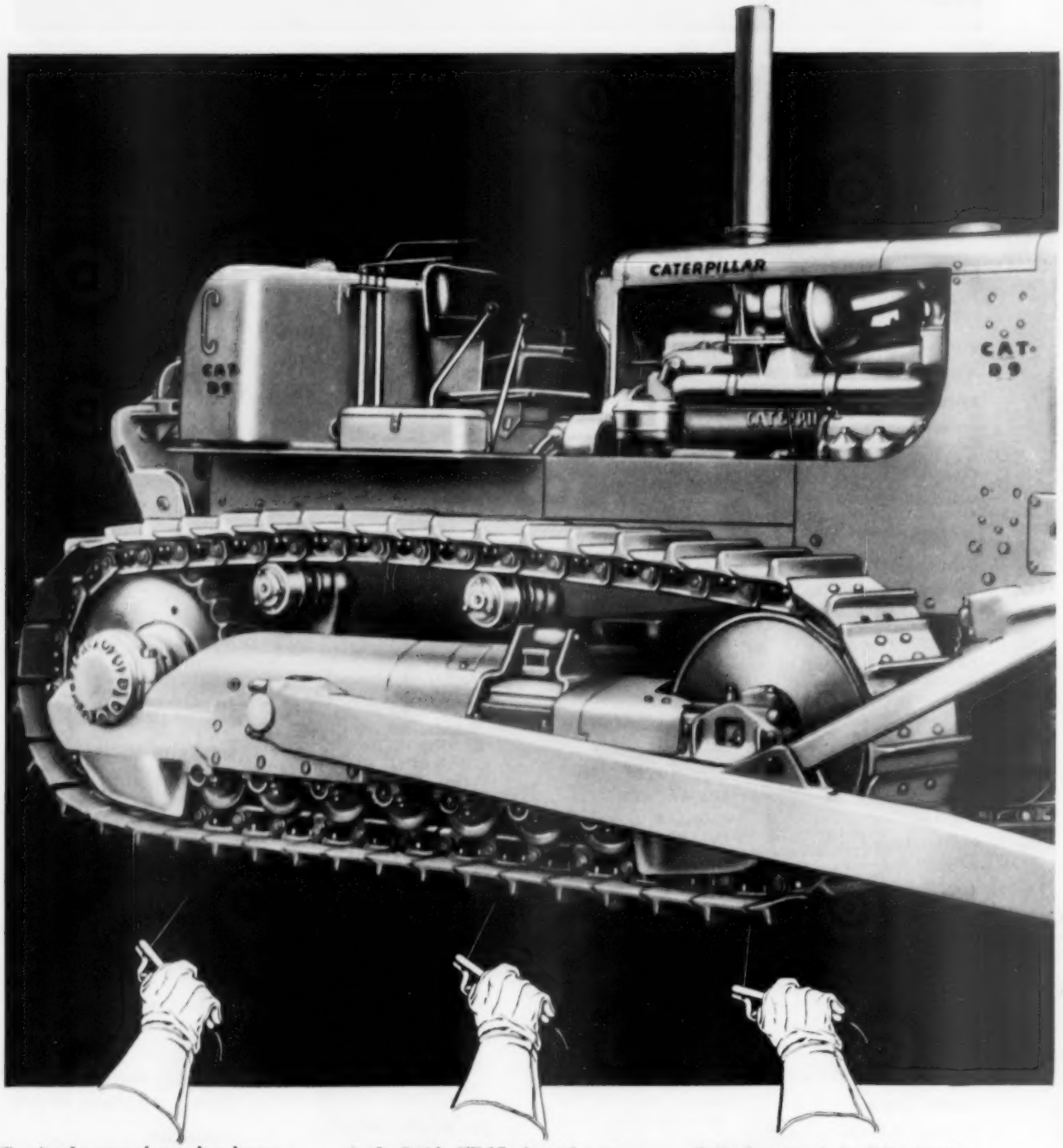
NEW ORLEANS

Blackwell, G. W. (C to B)

NEW YORK

Spraragen, William (B to E)

A. O. Smith welding



To give frame and gear box bonus strength—guts to withstand extreme punishment—Caterpillar welds with A. O. Smith's SW-35 electrodes. SW-35 (E-6020) electrodes produce 45° fillets without undercut, are ideal for downhand welding, too.

A. O. Smith SW-17 electrodes are used to complete the mighty D9's frame. These AWS Class E-6012 electrodes lend themselves to heavy production welding . . . possess greater built-in toughness than other electrodes in their class.

Critical parts of the D9's steering clutch case are welded with A. O. Smith SW-15 electrodes. High heat and high speed combine with standout performance in all positions to make these the finest universal electrodes in AWS Class E-6013.

electrodes help build world's biggest crawler tractor

CATERPILLAR D9 TRACTOR

Built for brutal service . . . this the mightiest of all crawler tractors delivers 230 turbo-charged horsepower at the drawbar.

Reliable performance on the world's toughest projects results from Caterpillar's engineering and manufacturing skill . . . unfailing insistence on quality. The company is famous for using only the finest materials and components. For years, they have been among the largest users of A. O. Smith electrodes.

NOTE! *A. O. Smith supplies three kinds of welding electrodes for use in the manufacture of the world's biggest crawler tractor — the D9. These electrodes are favorites with the welding operators who work on the D9 line. Of course, all three electrodes were selected after thorough testing . . . after they had proved their ability to produce Caterpillar-quality welds all the time.*

If welding quality is on your mind, too, it will pay you to investigate A. O. Smith electrodes. See the representative who calls on you . . . or write A. O. Smith Corporation, Welding Products Division, Milwaukee 1, Wis.

The man from A. O. Smith . . .

Erv Steidl is the representative who helped Caterpillar select the right A. O. Smith electrodes for use on the D9. Not just a salesman — but a trained welding consultant — the man from A. O. Smith will be glad to help you with your welding problems.

PRODUCED BY WELDERS FOR WELDERS

Because welding is our full-time business, we offer you America's top line of welder-proved electrodes, machines and accessories.

Through research . . . a better way

A.O.Smith

CORPORATION

WELDING PRODUCTS DIVISION
Milwaukee 1, Wisconsin

INTERNATIONAL DIVISION: MILWAUKEE 1, WIS.

ABSTRACTS OF CURRENT PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,711,959—Welding Steel for Developing High Surface Hardness Under Impact—William T. De Long, West Manchester Township, York County, and Gustaf A. Ostrom, Paradise Township, York County, Pa., assignors to The McKay Co., Pittsburgh, Pa.

This patent is on an article including a structure having a welded deposit thereon which develops high surface hardness under impact. The deposit also is free from excessive shrinkage and has a specialized composition including from 0.20 to 0.85% of carbon and specified quantities of various other alloying metals, the remainder of the deposit material being iron.

2,711,979—Process of Cutting Stainless Steel and the Like—John B. La Pota, Chicago, Ill., assignor to National Cylinder Gas Co., Chicago, Ill.

This method of cutting metal is for those materials normally immune or resistant to conventional flame cutting with preheat by oxygen alone. The new method includes the step of delivering a flux-forming material to the site of the cut in a stream of carrier gas comprising a mixture of a gas which is nonoxidizing under specific flame cutting conditions, and mixing at least 5% by volume of hydrogen with the mixture so that no more hydrogen is present than would render the mixture inflammable in air. This is done to improve the metal surface by reducing or preventing the formation of a tightly adhering scale thereon.

2,711,049—Electric Welding and Soldering Gun—Kark J. Aversten, Lidingo, Sweden, assignor to Svenska Aktiebolaget Gasaccumulator, Lidingo (near Stockholm), Sweden.

The patent is on an electric welding gun for welding a rod-shaped member to a base. The gun includes a time control arrangement mounted integral with the gun and including a welding circuit, a supply of fusible wire dimensioned to melt when the energy required for the welding process has been supplied, and jaws for removably connecting an end portion of the wire in series with the welding circuit.

2,712,175—Process of Brazing with Copper-Base Alloy—Louis E. Stark, Grand Island, N. Y., assignor to Union Carbide and Carbon Corp.

Stark's patent is on a process of brazing by depositing a copper base alloy on a metallic article. The process includes the step of melting in an inert shielding atmosphere an alloy containing 25 to 35% manganese, 1 to 10% aluminum, 0.1 to 2% silicon and the remainder copper and incidental impurities.

2,712,249—Machine for Flattening Internal Beads in Welded Tubing—Walter Siegerist, University City, Mo., assignor, by mesne assignments, to Continental Foundry & Machine Co., East Chicago, Ind.

In this patent, a machine is provided for positioning an anvil within a tube formed from skelp, while hammer means are provided on the outside of the tube for being periodically moved radially inwardly to flatten the internal bead of the tubing against the anvil. Other special control means are provided for the hammer and anvil and control action thereof.

2,712,585—Pneumatically Operated and Controlled Resistance Welder—Steven Domeny, Chicago, Ill., assignor to Ada Metal Products, Chicago, Ill.

This patent relates to an electric resistance welder that has a pair of opposed welding electrodes mounted in respective arms of the welding machine which electrodes are supplied with current from a conventional power supply source. One of the arms is pivoted to permit the electrodes to be brought together with substantial pressure in welding relationship. Certain control cylinders and cam means are provided in the apparatus for regulating the duration and pressures of the welding contact of the welding electrodes.

2,713,105—Spot Welding Apparatus—Fritz Gengenback, North Attleboro, Mass., assignor to Evans Case Co.

This patent relates to special spot welding apparatus wherein members are to be united together wherein one member has a surface ornamentation on one face thereof. One of the electrodes has a surface configuration similar to the ornamentation of the one piece but in inverse order for detachably locating the one member relative to the axis of the electrode. Tubular means are associated with the other electrode for detachably holding other members and locating them with relation to the first member for welding action.

2,713,106—Welding Apparatus—Perry C. Arnold, Chicago Ill., assignor to Chicago Bridge & Iron Co.

Arnold's patent is on apparatus for welding substantially horizontal edges of substantially vertically extending stationary plates and it comprises a chassis having rolling means mounted thereon for carrying the apparatus around the shell of a tank in a direction parallel to the upper edge of the plates. The chassis carries welding means thereon, and a motor is provided for driving the entire apparatus along the plates.

2,713,107—Welding Rod Holder—William D. White, Mount Ephraim, N. J.

White's patented welding rod holder includes an elongated casing having a welding rod receiving opening in a side thereof. A sleeve in the casing is in alignment with the opening for receiving the end of the welding rod, and a lever is positioned on the casing in such a manner as to obstruct the opening in the casing, and in a second position the lever is out of registration with the opening. Other control means are provided for completing the novel welding rod holder.

2,713,196—Method for Cladding and Product Resulting Therefrom—Robert L. Brown, Birmingham, Ala., assignor to Chicago Bridge & Iron Co.

Brown's patent on a method of forming clad plates includes the step of making a sandwich including a base plate and a cladding plate with a thin layer of brazing material between them, and the edges of the sandwich are hermetically sealed so that a vacuum of not less than about 18 in. of mercury is provided in the chambered area of the plate. The assembled plates are heated to a brazing temperature sufficient to melt the brazing material so that their contiguous surfaces move together under the existing vacuum condition in the chambered area. Then the assembled plates are cooled to below the melting range of the brazing material while maintained in the vacuum of the chambered area.

2,713,626—Welding Machine—Alfred H. Lewis, Huntington Woods, Mich., assignor to Swift Electric Welder Co., Detroit, Mich.

A novel welding machine is disclosed and it has paired sets of clamping electrodes with a first set of electrodes being stationarily positioned and being nonadjustable, whereas a second set of clamping electrodes is provided and is movably and adjustably positioned as a set with relation to the first set. A saddle is provided in association with the movable, adjustable set of electrodes, and other means are provided for rocking this saddle, which is associated with the movable electrodes and thus is movably associated with reference to the set of stationary electrodes.

NEWS of The Industry

Appointments Made by National Welding

Ernest L. Mathy, chairman of the board of directors, National Welding Equipment Co., San Francisco, Calif., advises that the board of directors has elected Henry L. Pohndorf, vice-president in charge of sales. Mr. Pohndorf, a graduate of Massachusetts Institute of Technology and in the welding industry for many years, has been chief engineer for the company, and recently, general sales manager. He has been with the company for some ten years.

Announcement was also made of the appointment of William G. Norman as chief engineer for the company.

Richard K. Brown has been elected treasurer, and Mrs. Lois J. Currier, assistant treasurer of the company.

Welded Bridge Stirs Wide Interest

In the July 14, 1955, Buffalo *Evening News*, there appears a very interesting story concerning the Erie Thruway Bridge being built over Cattaraugus Creek near Irving.

"It's not its size or length that's attracting attention," Mr. Wacker writes. "The five spans totaling 664 ft will look pretty much like any other bridge. It's the method of construction that's interesting.

"This bridge is believed to be the first major all-welded plate girder bridge to be built in the United States," explained Walter J. O'Brien, supervising engineer on the job for the State Department of Public Works.

"There have been other all-welded bridges but none have been major bridges. The others have been smaller units."

Inquiries began flooding in back in 1946 when plans were first publicized in a national magazine, *Engineering News Record*.

"We received letters, from everywhere in the world—even India—asking about the bridge," the white-haired veteran of 34 years of state service observed.

"We had to write and tell them it hadn't even been built, but Ernst Construction Corp. is fabricating the steel and will start erecting it in about a month."

Welded bridges are much cheaper to build, Mr. O'Brien explained, and the floor becomes part of the span, thus helping support its own weight load.

The bridge, for which three of four piers have been finished, is the largest of four being built in the twelve miles of thruway now under construction in Chautauqua County.

Union Carbide Presented Safety Award

For the second consecutive year, Union Carbide was presented the National Safety Council's Award of Honor on the basis of its 1954 safety record, which surpassed the corporation's all-time record set in 1953. The 1954 Award was given, as usual, for a reduction of frequency and severity rates below par rates established by the Council. During 1954, the accident frequency rate for corporation employees of 3.02 disabling injuries per million man-hours worked was 44% below the par rate. The severity rate of 0.58 day lost per thousand labor hours worked was 38% below.

In addition to the corporation's award, several individual plants have received Awards of Honor. A total of 133 awards were received from the National Safety Council in recognition of plant, home and off-the-job safety programs, and 118 awards were received from national, state and local agencies.

The K-G Equipment Co. (Inc.)

The K-G Equipment Co. Inc., Allentown, Pa., announce the appointment of the following distributors for their industrial equipment: National Welding Supply Co., 457 St. Michaels St., Mobile, Ala.; Tex-Air Gas Co., 211 Buchanan St., Amarillo, Tex.; Sanders & Stewart, 608 E. Pearl St., Jackson, Mich.; W & S Service Engineers, 1113 N. Hickory St., Joliet, Ill.; Welders Service & Supply Co., 2164 Spring Grove Ave., Cincinnati, Ohio.

WW Alloys Distributor

Bond Welding Service Co., 1120 Cobbs St., Drexel Hill, Pa., has been appointed authorized distributor in the greater Philadelphia area for resistance welding electrodes, water-cooled holders, seam welding wheels and other resistance welding materials made by WW Alloys, Inc., division of Fansteel Metallurgical Corp., according to an announcement by Richard I. Allen, sales manager.

Miss Mullarky Retires

The Champion Rivet Co. advises that Miss Mazy C. Mullarky resigned after 56 years of faithful and efficient service. Miss Mullarky was employed in 1899 by the late David J. Champion who founded the company in 1895. She was employed as private secretary to Mr. Champion and continued in that capacity under the current president, T. Pierre Champion. Miss Mullarky started her service at a time when it was the exception rather than the rule for women to be employed in office work.

COMING EVENTS

A Calendar of Welding Activity

AWS National Meetings
1955 Fall Meeting:
October 17–21, Bellevue-Stratford Hotel, Philadelphia, Pa.
1956 Spring Meeting:
May 8–12, Statler Hotel, Buffalo, N. Y.

NEAWSD Meetings:
First Fall Meeting, Tuesday, September 20.
Hampton Court Hotel, Brookline, Mass.

NWSA Meetings:
Central Zone
September 19 and 20, Morton House, Grand Rapids, Mich.
Luncheon Meeting, October 18, Bellevue-Stratford Hotel, Philadelphia, Pa.

RWMA Meetings:
September 23,
Westinghouse Electric Corp., Buffalo, N. Y.
October 18th, Philadelphia, Pa.

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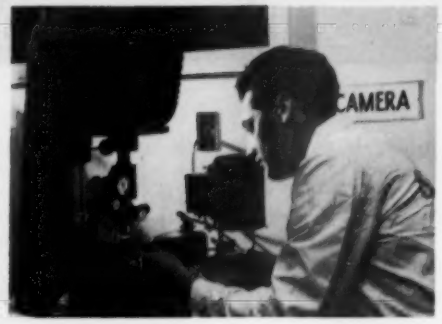

Low Temperature EUTECTIC WELDING ALLOYS

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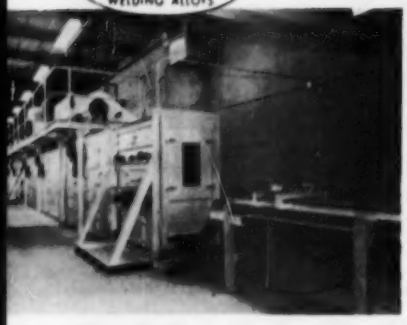

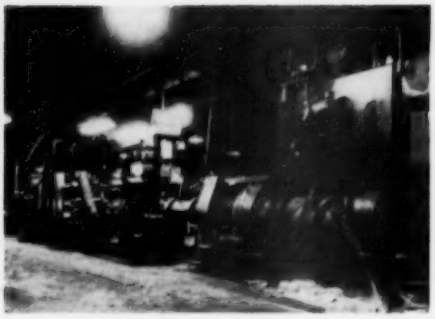

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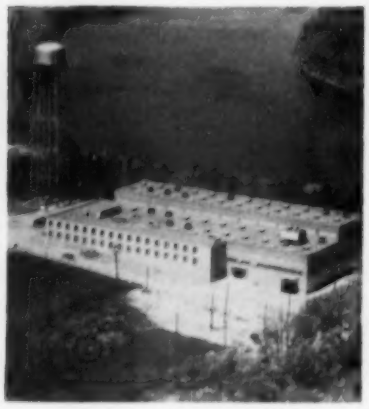
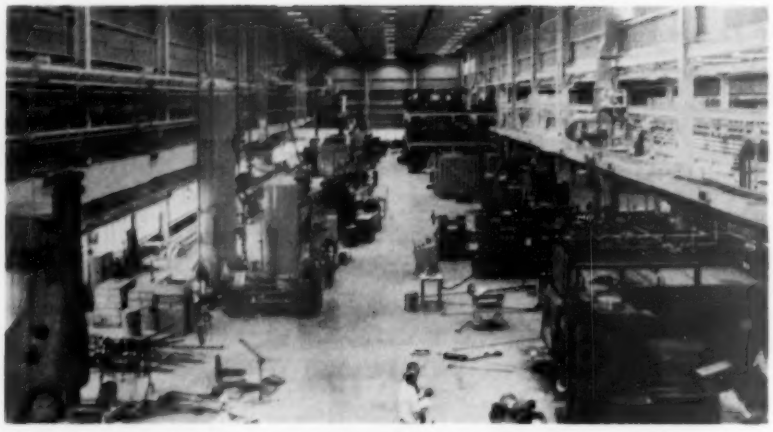
GE DEDICATES NEW RESEARCH BUILDING



Aerial view of the new Metals and Ceramics Building at the General Electric Research Laboratory. The new building, which is located at The Knolls overlooking the Mohawk River near Schenectady, N. Y., was formally dedicated Aug. 26, 1955



Interior view of high-bay area of the new Metals and Ceramics Building at the General Electric Research Laboratory. "Mezzanines" (center background), supporting pilot-plant apparatus, are tied to the main structure of the building. At the left corner of the building in the background is the one-ton, three-phase arc furnace. Other major items of equipment visible from the picture include a large drop forge hammer and an extrusion press (on the left), and the reversing rod and strip mills and industrial heating equipment on the right

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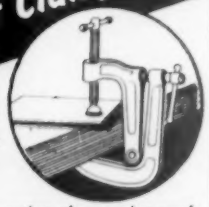

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	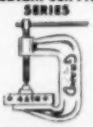	
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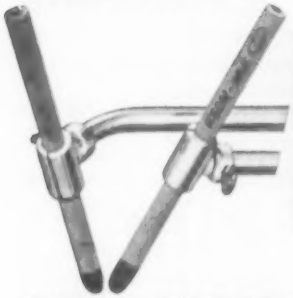

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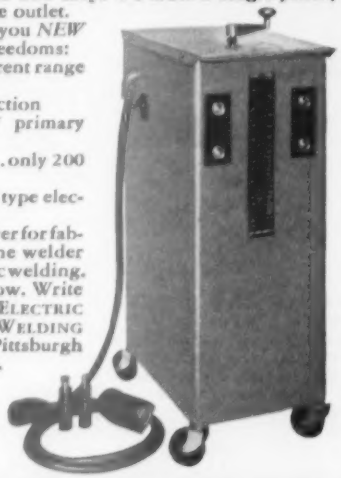

YOU CAN BE SURE...IF IT'S Westinghouse

Baltimore Distributors Form Local Organization

A meeting was held on July 6, 1955, at the Park Plaza Hotel in Baltimore and the Baltimore Welding Supply Association, Inc. was founded. The speaker of the evening was A. C. Axtell of Essex Welding Equipment Co., Newark, N. J. and vice-president of the National Welding Supply Association, who gave the group the benefit of his wide experience in forming a similar Association in the Newark area.

The following officers and directors were elected:

President—A. C. Earlbeck, Earlbeck & Landrum, Inc.
Vice-President—L. H. Mathews, Guilford Welding Supply Co.
Secretary—W. C. Landrum, Earlbeck & Landrum, Inc.
Treasurer—Fred Gaebler, Prest-O-Baltimore Co.
Directors—Fred Blenckstone, Baltimore Oxygen Supply Co.; Randy Cooke, Arundel Sales & Service Co.; Harry Fountain, Welders Supply Co.

In addition to the welding supply houses represented by the officers and directors, the following are charter members of the group: Chesapeake Welding Supply Co., Harris Calorific Sales Co., Hobart Welder Sales Co., Southern Oxygen Co., Trippe Supply Co., and R. S. Weiss Co.

The objectives of the Association are:

1. To encourage cooperation among distributors.
2. To inspire to observance of high business methods.
3. To promote the place and identity of the welding distributor as an essential link in the distribution of welding equipment, supplies and gases.

The 12 local welding supply houses in this new Association represent over 92% of the distributors in the Baltimore trading area.

ORGANIZATION MEETING HELD BY BALTIMORE DISTRIBUTORS

Charter members present at the July 6th meeting of Baltimore Welding Supply Association (*left to right*): Bob Donovan, Harris Calorific Sales Co.; Milton Phelps, Arundel Sales & Service Co.; Fred Blenckstone, Baltimore Oxygen Supply Co.; W. C. Landrum, Earlbeck & Landrum, Inc. (Secretary); Harry Fountain, Welders Supply Co.; Dick Carlin, Chesapeake Welding Supply Co.; A. C. Earlbeck, Earlbeck & Landrum, Inc. (President); A. C. Axtell, Essex Weldng Equipment Co. (Guest Speaker); Harry Weaver, Prest-O-Baltimore Co.; Fred Gaebler, Prest-O-Baltimore Co. (Treasurer); Clyde Adams, Southern Oxygen Co.; Randy Cooke, Arundel Sales & Service Co.; L. H. Matthews, Guilford Welding Supply Co. (Vice-Pres.)

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Navy Initiates Project on Ship Components

The Bureau of Ships has initiated a project to simplify the engineering drawing practices used to graphically delineate ships and ship components, according to a recent announcement made by Rear Admiral A. G. Mumma, Chief of the Bureau. This project has been undertaken because, now as never before, there is urgent demand for increasing the personal effectiveness of technical people, not only because of their acute scarcity but also out of economic and military necessity.

Because of the extremely broad technical spread of ship components, it is felt that this project will be of special interest to the entire engineering profession. Therefore, the Engineering Societies are being approached for aid in developing information to be used in this program.

The methods and techniques used within some companies are available; however, it is considered important to the general program to accumulate as much information as possible from others who have worked in this field.

Specifically, the Bureau would like to receive the names of organizations and people in the various technical fields who have developed papers, systems or techniques covering Simplified or Functional Drafting. Also the Bureau would welcome any other information which may be deemed useful to the project which may now be available or be developed in the future.

This project has been assigned to the San Francisco Naval Shipyard for detailed development. Communications relative to this matter should be addressed as follows: Commander San Francisco Naval Shipyard, attention: Design Division, Code 242P, San Francisco 24, Calif.

●

Victor Acquires Additional Property

L. W. Stettner, president, announces that Victor Equipment Co. has purchased property at 840 Folsom St., San Francisco. This property has been occupied by Victor for several years. Major remodeling work will begin Aug. 1, 1955, giving Victor new, modern offices and additional manufacturing space.

Mr. Stettner also informs that excellent progress has been made on Victor's new Alloy Rod & Metal Division plant, a 6-acre site located in Los Angeles, Calif.

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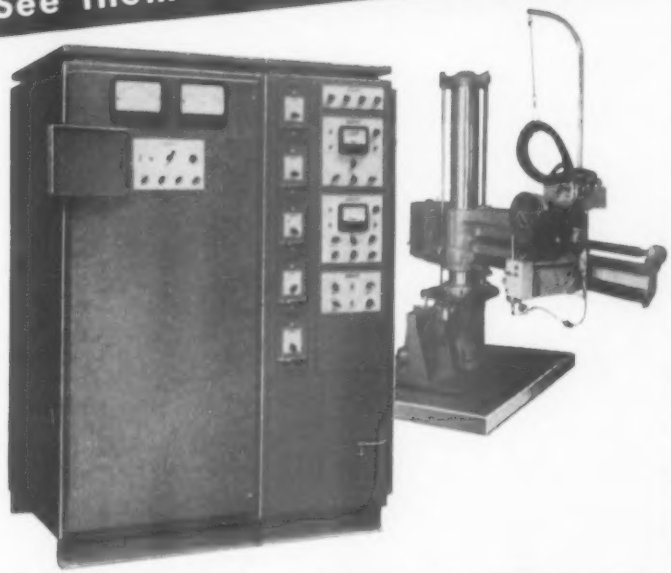

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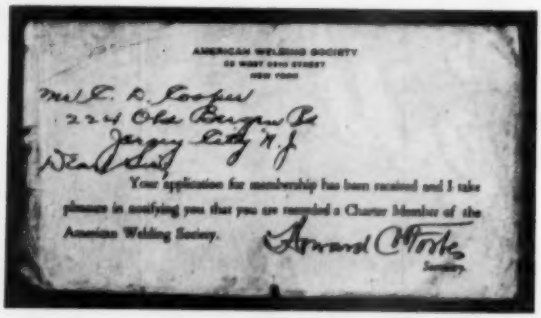
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Kroft Appointed General Superintendent

Frederick C. Kroft has been appointed general superintendent of manufacturing for Haynes Stellite Co., a Division of Union Carbide and Carbon Corp., according to an announcement by F. T. McCurdy, vice-president of manufacturing operations of the company.

Frederick C. Kroft

Mr. Kroft received a B.S. degree in Metallurgy from Purdue University in 1942. He joined Haynes Stellite Co. in 1936 and was employed in the grinding department and later in the chemical laboratory. In 1942 he became a junior research engineer, in 1950 assistant technical director, and later that same year superintendent of the inspection, process, and quality control department.

During World War II Mr. Kroft helped to establish a metallurgical testing group at Haynes Stellite Co. This group assisted in the development of high-temperature alloys used in gas turbine blades and other applications where materials with strength and corrosion resistance at elevated temperatures are essential.

Mr. Kroft is a member of the American Welding Society, American Society of Metals and the American Society of Testing Materials.

Munn Appointed District Manager

R. H. ("Hase") Munn, Jr., has been appointed district manager of the Pittsburgh area for WW Alloys, Inc., division of Fansteel Metallurgical Corp., Richard I. Allen, sales manager, announced.

Mr. Munn not only supervises sales of WW Alloys' aluminum bronze castings and forgings, but also the resistance welding electrodes, water-cooled holders, seam welding wheels and high conductivity copper alloy castings and forgings made by WW Alloys.

Briggs on Sales Staff

John G. Seiler, executive vice-president and general sales manager of Tube Turns, Louisville, Ky., has announced the appointment of Frank Briggs to the staff of the company's sales development department. Mr. Briggs joined Tube Turns, a division of the National Cylinder Gas Co., in 1942 and became a member of the plant's accounting and materials section. He was transferred to the sales department in 1947.

EMPLOYMENT SERVICE BULLETIN

Position Vacant

V-326. Well-known electrode manufacturer wants hard surfacing Sales Engineer. Must be free to travel extensively. Some knowledge of welding metallurgy and actual welding experience preferred. Good advancement possibilities. Salary open. Submit full résumé. Replies will be held in strict confidence. Our own employees know of this ad.

Services Available

A-679. Graduate Welding Engineer with M.S. degree desires position in industry with responsibility and good opportunity for advancement. He prefers work in the development and application of welding to production, particularly resistance welding. He has eight years' experience in welding research and development and in industry, and he is a registered professional engineer. His experience includes the welding of titanium, zirconium and molybdenum. He is 32 years old. This man will relocate.

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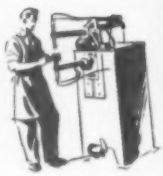

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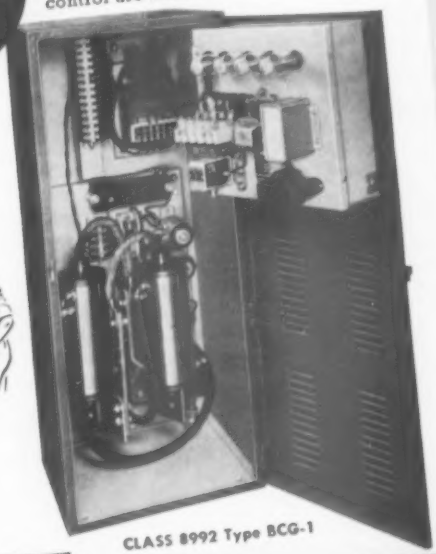

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NEW LITERATURE

Brazing Manual Published by AWS

The Committee on Brazing and Soldering of the American Welding Society, comprising the top experts of the country, has prepared a long-needed book on the subject of brazing. The appearance of the *Brazing Manual* marks a milestone, for now brazing takes its place with other branches of engineering which have had handbooks published in their respective fields.

While brazing has been used since Biblical times, not until now has there been available a compilation, in a single source, all the technical information on the eight brazing processes. Complete and valuable information is given on each of these processes: Torch Brazing, Twin-Carbon Arc Brazing, Furnace Brazing, Induction Brazing, Resistance Brazing, Block Brazing and Flow Brazing. Every conceivable metal of industrial importance, common and rare, is described in terms of techniques of brazing including equipment, precleaning, surface preparation, postbraze cleaning and inspection. An invaluable appendix in convenient chart form lists properties of brazable metals and alloys such as aluminum, copper, brass, bronze, silver, nickel, gold, platinum, iridium, rhodium, palladium, tantalum, irons, steels, etc.

For the first time anywhere brazing fluxes and atmospheres are standardized into types according to their compositions and uses. In addition to production and design information, there are extremely valuable sections on testing, quality control, safety, health protection plus detailed explanations of causes of defects and how to correct them. The entire metals and metalworking industry will welcome this new and practical sourcebook of essential information.

Copies are immediately available, at the list price of $4.75, from the American Welding Society, 33 W. 39th St., New York 18, N. Y., or from Reinhold Publishing Corp., 430 Park Ave., New York 22, N. Y.

Inert Arc Welding

The Air Reduction Sales Co. has just announced the availability of a new 16-page catalog featuring Aircomatic (Airco's inert-gas-shielded metal arc welding process) welding equipment. Manual and automatic units along with accessory apparatus and welding wire are included in the illustrated booklet.

A copy may be obtained by writing, Air Reduction Sales Co., 60 E. 42nd St., New York 17, N. Y.

Resistance Welding

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., introduces Vol. 4, No. 6, *Resistance Welding At Work*, covering the case history of mash welding double sinks with a Sciaky 250 kva Mash Welder.

For a complete description of mash welding process, plus "on the spot" photographs, write direct to the Chicago plant for copies of Vol. 4, No. 6.

Stainless Steel Design Handbook

Alloy Metal Wire Division, H. K. Porter Co., Inc., announces the availability of its new 40-page *Design Handbook*. The handbook covers in detail the properties, specifications, applications and design data on the Division's 20 grades of stainless steel wire, rod and strip.

Copies of the handbook can be obtained by writing to E. H. Mann, Sales Manager, Alloy Metal Wire Division, H. K. Porter Co., Inc., Prospect Park, Pa.

Properties of Stainless Steel Welds

Engineers associated with the design and use of stainless tubular products in steam superheaters, heater coils in oil refinery heaters and petrochemical plant and high-temperature piping installations will be interested in a new data card issued by the Tubular Products Division of The Babcock & Wilcox Co. This data card gives the stress rupture properties of chromium-nickel stainless steel weld deposits.

Copies of the data card, known as TDC-178, are available free upon request to the division's general sales offices at Beaver Falls, Pa.

Stud Welding

Architectural details illustrating use of the stud welding method of fastening to reduce the cost of spandrels and window-wall installation are shown in a folder just published by Nelson Stud Welding Division of Gregory Industries, Inc., Lorain, Ohio.

Installations shown include the fastening details on the Equitable Life Assurance Building, San Francisco; Denver Mile High Center Office Building; UN Secretariat Building and San Francisco Municipal Airport Building.

For your copy, write directly to the company.

Automatic Weldment Cleaning

A new bulletin on the subject of automatic cleaning of rust, flux and spatter from welded pieces by abrasive blast cleaning has just been published by American Wheelabrator & Equipment Corp., 1332 S. Byrkit St., Mishawaka, Ind.

A complete case history is given concerning the effect of automatic blast cleaning on this work at a large manufacturing shop. It has illustrations of the equipment and typical parts that are cleaned.

Free copies of this bulletin are available from American Wheelabrator. Ask for Bulletin PE-6.

Hard-Surfacing Rods

A folder describing their new tungsten carbide hard-surfacing weld rods is being made available by Coast Metals, Inc., Little Ferry, N. J. For your copy, write directly to the company.

Brazing News

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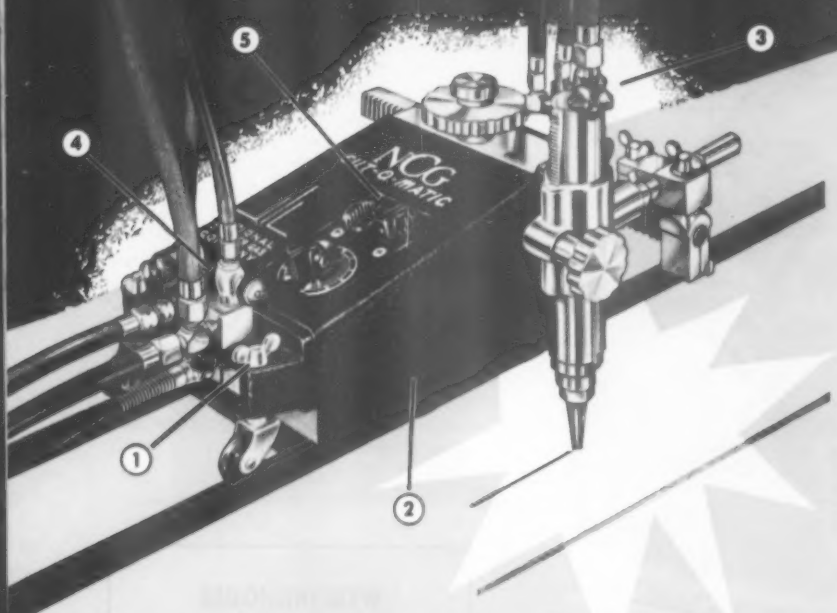

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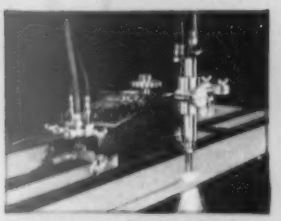

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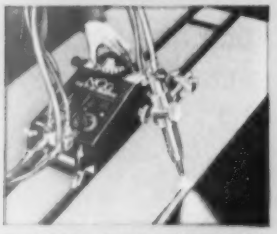

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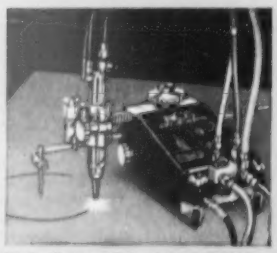

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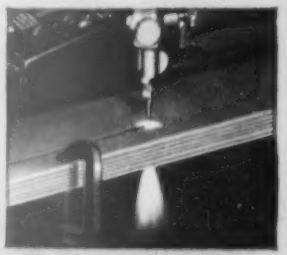

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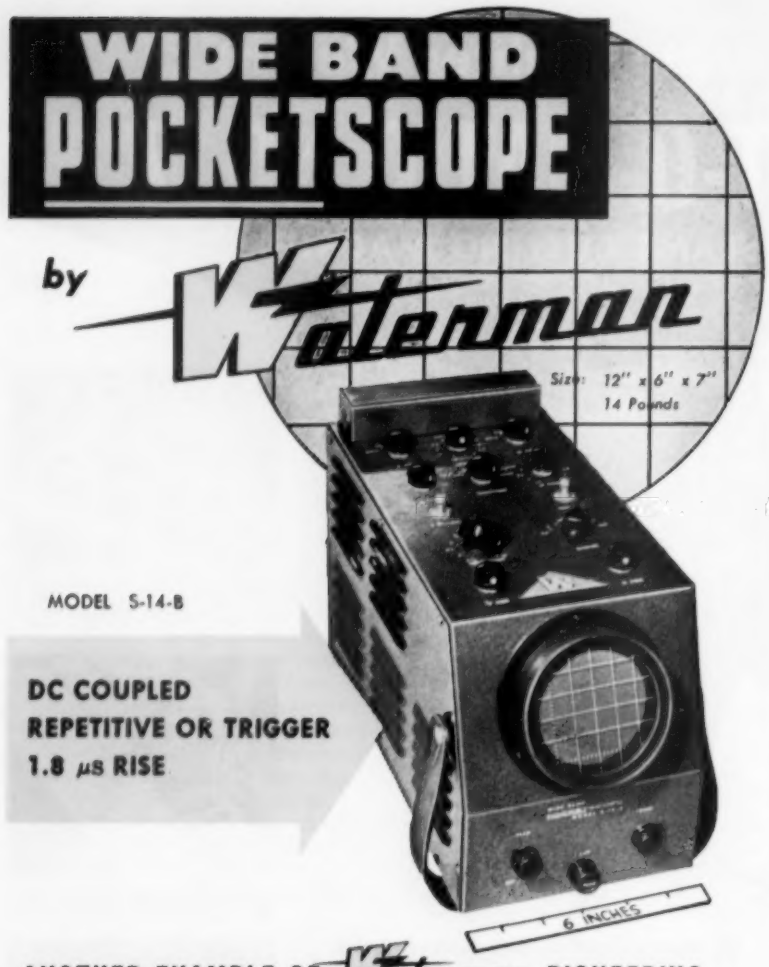

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Temperature Brazing News," No. 69. For your copy write directly to Handy & Harman, 82 Fulton St. New York 38, N. Y.

REVIEWS OF NEW BOOKS

Tool Design

TOOL DESIGN by William R. Jeffries, Montana State College. Published by Prentice Hall, Inc., 70 Fifth Ave., New York 11, N. Y. (1955). 217 pages. Profusely illustrated. $6.35.

Comprehensive, clear and easy to follow, this book constitutes an authoritative introduction to the whole field of tool design.

It gives actual instructions on how to design machine tool jigs and fixtures, *weld* and assembly fixtures, punch press tooling and hydraulic press tooling. The author also explains pressworking machines and tools, and devotes an entire chapter to clamps and methods of clamping and locating.

Helpful reference appendices provide actual data on drafting practice and other useful information.

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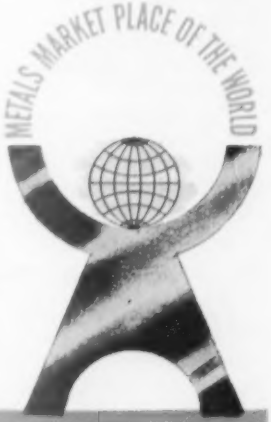

OCTOBER 17 thru 21 1955

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NEW PRODUCTS

Alloy Fluxes

The Lincoln Electric Co. of Cleveland, Ohio, has introduced a new line of agglomerated fluxes for automatic submerged arc welding of low-alloy steels. The method, claimed to be a new approach to the problems of welding complex and highly specialized steels, is said to be both economical and flexible in meeting the wide range of special requirements in welding these steels.

The heart of this new approach to welding alloy steels is an alloy flux individually compounded for each job. These special fluxes, through a process of agglomeration, contain alloying elements which may be changed as required by the particular job for which the flux is made. These new alloy fluxes can be used with a mild steel electrode and will produce a low-alloy deposit which can be readily varied to meet specific requirements.

For additional information, write directly to the Lincoln Electric Co., Cleveland 17, Ohio.

Low Hydrogen Electrodes

Pacific Welding announces that it has placed its new series of "Low Hydrogen Electrodes" on the market. Recommended especially for welding armor and high-strength alloy steels, these new electrodes are designed for all-position welding. They comply with ASTM-AWS specifications 100-15-16 and 120-15-16.

For descriptive literature, price list and samples of the new Pacific Low Hydrogen Electrodes, write to Pacific Welding Alloys Mfg. Co., 310 North Ave. 21, Los Angeles 31, Calif.

Hard-Facing Electrode

Development of the new Mir-O-Col 4T-S Hard Facing Electrode has been announced by MIR-O-Col Alloy Co., Inc., of Los Angeles, Calif.

MIR-O-Col 4T-S is especially recommended on tractor track rails, grousers and all-around general build-up of worn tractor rails.

Descriptive literature, price list and samples of MIR-O-Col 4T-S are available on request. Write to MIR-O-Col Alloy Co., 312 North Ave. 21, Los Angeles 31, Calif.

Arc Time Totalizer

The development of a new Arc-Time Totalizer which accurately registers the time an arc current is in use is reported by Exline Engineering Co. of Tulsa, Okla.

According to the manufacturer, this new timer operates with an accuracy never before attainable due to its simple no-moving-part construction. Completely foolproof, a particular advantage is that current leakage, or any current below a specified amount, will not register on the clock.

Currently being introduced to the welding industry, pilot models of the new arc timer have been undergoing extensive industry tests for the past year.

Galvanizing Repair Stick

A new and improved metal alloy stick called ReGalv is now available for regalvanizing any area where the zinc coating (of galvanized sheets, shapes, fittings, etc.) has been damaged or destroyed by welding, flame-cutting, bending or the like.

The regalvanizing metal stick is said not to crumble, and therefore may merely be rubbed over the weld or other hot surface to be recoated. It melts and bonds at temperatures as low as 600° F.

For further information, write to RotoMetals, 980 Harrison St., San Francisco 7, Calif.

Aluminum Welding Cable

A new, lightweight aluminum welding cable called Tweco-Lite, has just been announced by the Tweco Products Co. of Wichita, Kan. Weighing only one-half as much as conventional copper welding cables, the new cable is said to have great flexibility which makes it easier and less tiresome for the welders to do their jobs.

Tweco-Lite Aluminum Welding Cable is made in Grade "SR," which has a synthetic rubber jacket, and Grade "N-60," which has a 60% neoprene jacket reinforced with rayon braid. Grade "SR" is recommended for normal use and the Grade "N-60" where oil, heat, chemicals abrasion and rough usage are encountered. Both grades of cable are made in No. 4 through No. 4/0 sizes.

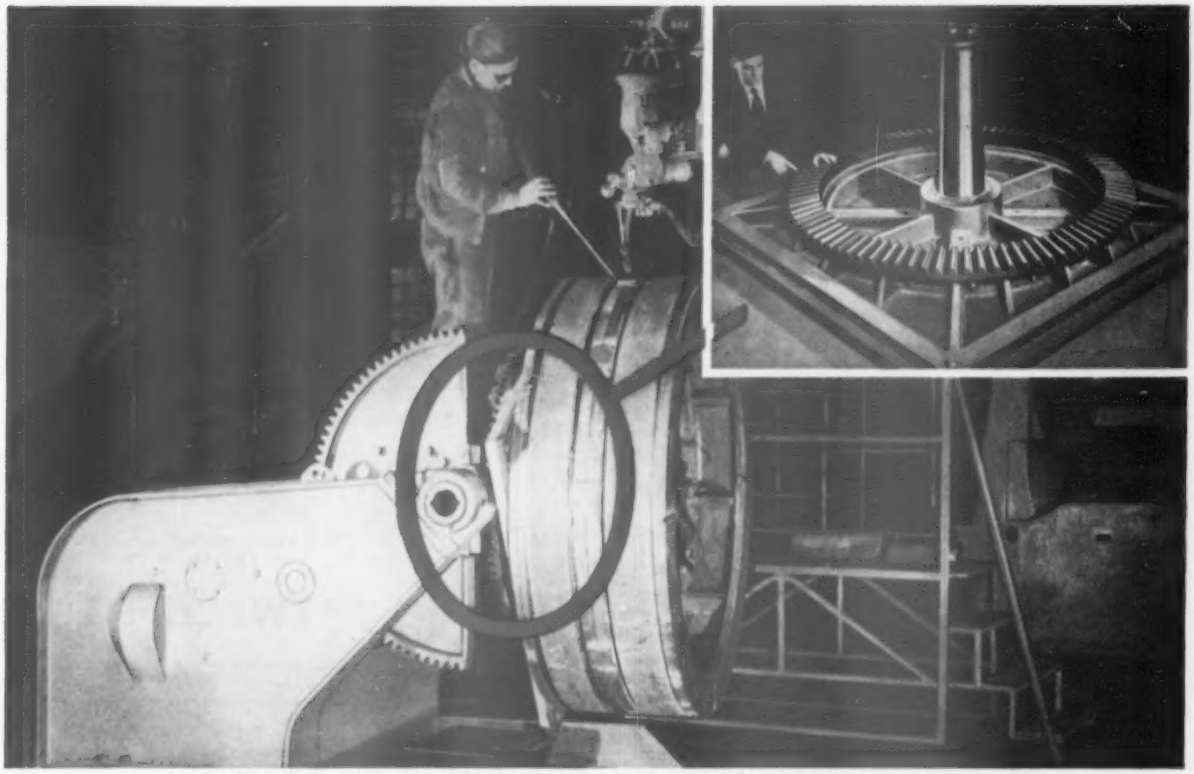
The cable is conveniently packaged in precut 50- and 100-ft lengths; it is also available in 250- and 500-ft coils as well as 1000 ft reels.

For further information, write directly to Tweco Products Co., P.O. Box 666, Wichita 1, Kan.

Welding Positioner

The Weldma Co., of West Hartford, Conn., has developed a newly designed circumferential welding positioner of 4000 lb capacity which according to the company, incorporates the most modern and advanced engineering concept of "Machine Tool Quality" precision welding positioners.

The positioner frame, mounted on



AUTOMATIC WELDER AT WORK. Inset shows extra-large spindle and rugged bull gear of Worthington Welding Positioner, rated at 24,000 pounds, continuous duty. Worthington's positioner ratings are not just a by-product of the design. Rather, they are the result of extensive tests in laboratory and field.

Double-check welding positioner ratings!

— *don't underestimate their importance*

FREE New Bulletin R-1700-B15 shows you how to choose a positioner. It answers the seven basic questions you should ask before you buy *any* positioning equipment.

In positioners, as in all Worthington products, ratings result from extensive tests in the laboratory and on the job.

Here are two steps that we take to assure you that every Worthington positioner rating is a *job* rating:

1. Each model is proved on the job before marketing as a standard unit.
2. Each positioner part, including motors, gears, and bearings, is selected to give you extra capacity for overload conditions.

Extra capacity, honestly rated, is typical of *all* Worthington Positioners — from smallest to largest, rated up to 50 tons.

No matter what your size requirements, there's a Worthington Welding Positioner for your job. Like all other Worthington products, its rating is backed by the integrity and reputation of 114 years of engineering experience. That's why you can rate our ratings as "conservative." Write for new Bulletin. Worthington Corporation, Harrison, New Jersey. V-59

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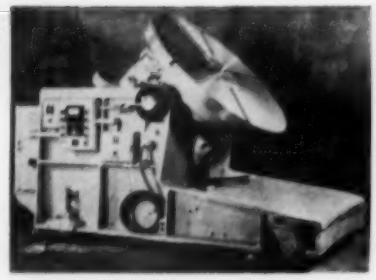


grooved casters, is of ribbed cast iron construction for maximum strength and rigidity. The 52-in. diameter keyed face plate with center locating pin is set in a large Timken bearing to eliminate drag and is driven by a gear reduction motor.

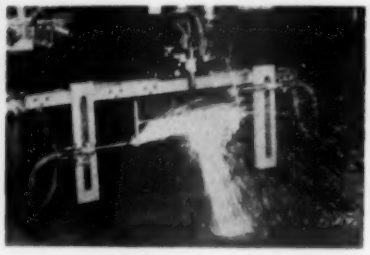
All inquiries on this equipment should be forwarded to The Weldma Co., 62 LaSalle Rd., West Hartford, Conn.

Flame Saw

A new type of Flame Saw for cutting heavy steel structurals has just been announced by The K-G Equipment Co., Inc., of Allentown, Pa.

According to the manufacturer, the Flame Saw is competitive in efficiency to mechanical sawing units. It is hand-portable and can be set up anywhere in the plant or on the job. It is said to reduce handling, setup and operating costs, and to increase speed and accuracy of cutting of structurals at any angle.

It requires only 110-v power supply



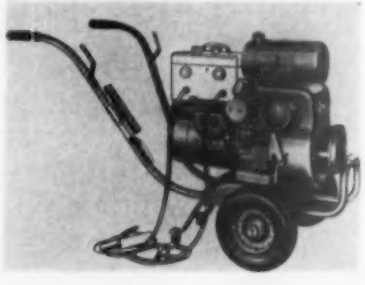
and a setup of oxygen and fuel gas. The work itself is the table.

For complete information write directly to company at above address.

Portable Generator

A new, lightweight, 2500-w Onan Electric Generating Plant, designed with special external modifications to operate any transformer type a-c welder up to 150-amp rating, is announced by D. W. Onan & Sons Inc., Minneapolis, Minn.

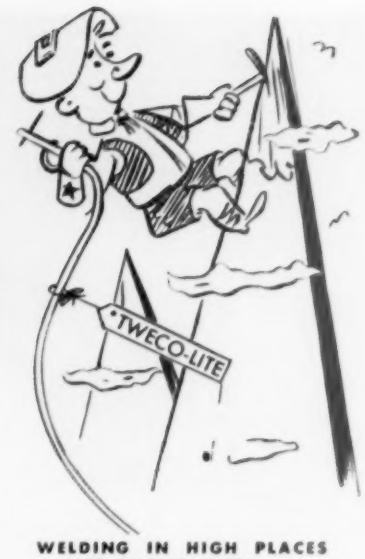
Designed as a portable source of a-c power, the unit combines the new air-cooled Onan Model 205AJ-1M, 2500-w, 115-v, 60-cycle, AC Electric Generating Plant with any 150-amp transformer type welder to become a versatile generator-welder "package" for construction and building jobs, repair shops, farms, ranches, service stations, public utilities and railroads.



This completely self-contained portable generator is available mounted also on a lightweight, two-wheel dolly or a welded tubular steel carrying frame. For complete information write to D. W. Onan & Sons Inc., Minneapolis, Minn.

Eye Shield Attachment

A detachable shield which can be clipped to the inside lens holder of any standard welding hood to eliminate light coming in from the side or rear is



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CONTROLARC automatic control is made possible by the patented Vickers Self-Saturating Magnetic Amplifier with exclusive voltage-sensing, feed-back control circuits. Constantly analyzing welding voltage and automatically adjusting welding current to arc changes. Vickers Magnetic Amplifier maintains ideal arc characteristics, through the entire welding range, under all welding conditions.

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Hardness, Rockwell B	72 to 78
Face Bend	180°
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Bronze-Arc C conforms to A.W.S.—A.S.T.M. Specification No. B-225-53T, Classification E CuSn-C.

Write today for complete information on new Bronze-Arc C electrodes—the electrodes designed especially for high strength, X-Ray quality welds.

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no finer electrodes made . . . anywhere

now available from Byers Supply Co of Oakland, Calif.

Known as the Sight-Guard, the shield is molded from sponge rubber on a flexible frame with clips which snap onto any standard lens holder. Weight is only 2 oz; ventilation is provided to prevent fogging and assure complete comfort.

Literature and prices may be obtained by writing Byers Supply Co., 3124 E. 14th St., Oakland, Calif.

●

Welding Positioner

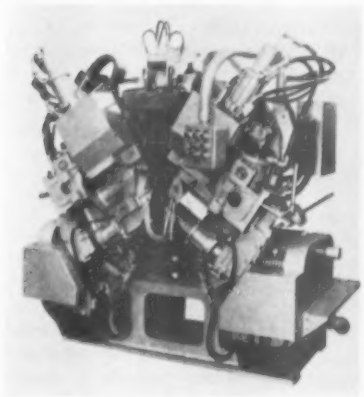
A new, universal-balance welding positioner has recently been put into production by the P&H Welding Division of the Harnischfeger Corp. Designated as the UP-1000, the unit positions pieces weighing up to 1000 lb for downhand welding. It occupies a floor area of 22 × 26 in. and weighs only 200 lb.

Even though the UP-1000 does not operate by means of electric motors, Harnischfeger claims that its operation is extremely simple and practically foolproof.

Further information about the new P&H UP-1000 Welding Positioner is available from the Harnischfeger Corp., Milwaukee 46, Wis., or any of its welding sales offices or distributors.

Double End Welder

The double end welder, a new development in resistance welding equipment by the Taylor-Winfield Corp., is said to cut production time and costs by simultaneously joining both ends of a strip of steel to a forging to make automatic transmission bands for automobiles.



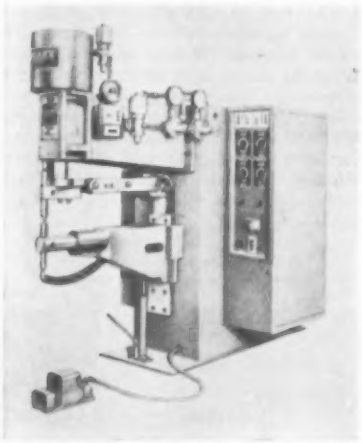
Flash-butt welding of rolled steel band stock to high-strength forgings produces low-cost bands of lasting strength. Welding both ends at once cuts production costs even more.

The new Taylor-Winfield double-end welder is adjustable for many band sizes and forging shapes. For complete details write to Taylor-Winfield Corp., Warren, Ohio.

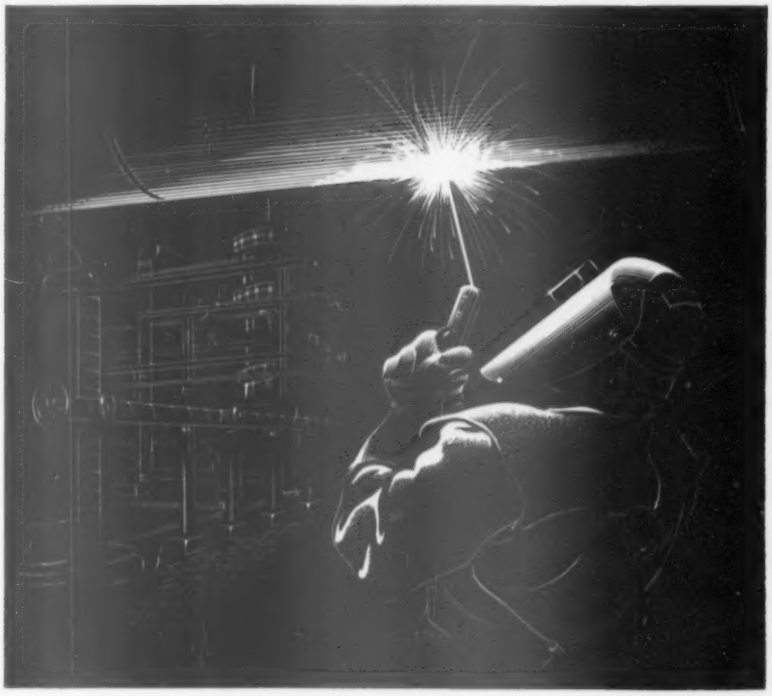
Spot Welder

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., announce the new design of a standard, air-operated, press type Three-phase Modu-Wave PMCO 28T Spot Welder.

This welder is one of a series of sizes ranging from 08T through 78T. Available in 100 kva at 50% duty cycle, the PMCO 28T can be supplied with either 36-, 48-, or 54-in. throat depth and elec-



New ARCALOY
502 LIME ELECTRODES
for x-ray quality welds
in 4 to 6% Chrome Steels



New Arcaloy 502 lime electrodes produce welds of required x-ray quality in 4 to 6% chrome steels as required for high temperature, high pressure services. Arcaloy 502 electrodes have a new lime, low hydrogen type coating, *a coating that will not fingernail*, eliminates underbead cracking and provides a basic slag with a scavenging action that consumes impurities.

The New Arcaloy 502 lime electrodes have excellent weldability *in all positions*, with an arc action smooth and stable, an easy-to-control weld metal and slag. Arcaloy 502 electrodes conform to A.W.S. A5.4 and A.S.T.M. A298-55T Specifications, Grade E502-15. Try the New Arcaloy 502 electrodes for x-ray quality welds in your high temperature, high pressure applications. Contact your Alloy Rods Distributor or write the plant for all the facts.

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The PMCO 2ST reportedly will make consistent high quality welds on large production runs on a wide range of low carbon steel, austenitic steels, aluminum and magnesium alloys, air hardenable steels and stainless steel, all to Military Specifications, MIL-W-6858.

For more complete information of the PMCO 2ST, write to Public Relations, Dept. M-2 at the Chicago address, requesting Bulletin 334-3. For illustrated stories of typical applications of the PMCO 2ST in aircraft welding, request free copies of "Resistance Welding at Work," Vol. 3, No. 4; Vol. 4, No. 4; and Vol. 4, No. 5.

Welding Ring

A new flat type welding ring with 1/8-in. diameter spacer pins has been introduced by Tube Turns, Louisville, Ky., a division of the National Cylinder Gas Co. According to the manufacturer, it was developed to improve the submerged arc welding of pipe and fittings, but is also suitable for many manual welding applications. Reportedly, it enables joints to consistently pass X-Ray and Gamma-ray inspection, and to meet all safety code requirements. For further information write to Tube Turns, 224 East Broadway, Louisville 1, Ky.

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Ohio Valley Electric Corporation and its subsidiary, Indiana-Kentucky Electric Corporation, agreed to supply electric power to this great new atomic production center . . . *15 billion kilowatt-hours annually.*

Two huge generating stations are being built: The Kyger Creek Plant at Cheshire, Ohio will have five generating units of 200,000 KW each; Madison, Indiana's Clifty Creek plant will produce 1,200,000 KW with its six big generators. Each of the eleven turbo-generating units will operate off a single boiler, with super-heated steam reaching 1,050 degrees Fahrenheit under 2,000 lbs. pressure per square inch! The high temperatures and reheat, which the sponsor companies have pioneered in their own right, will make these two plants among the most economical, most efficient generating stations in the country.

Miller Selenium Rectifier d-c Arc Welder, in four wide-range models. For complete, concise, worthwhile information on these and other Miller Arc and Spot Welders, contact us today . . . at no obligation, of course.

These same elements, however, make the fabrication of faultless high-pressure steam lines vital . . . and extremely difficult. To accomplish this extremely important task, pipe welding specialists equipped with the maximum of engineering skill utilized the Miller Selenium Rectifier d-c Arc Welder.

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W. SPRARAGEN, DIRECTOR

WELDING RESEARCH COUNCIL
OF THE ENGINEERING FOUNDATION

SUPPLEMENT TO THE WELDING JOURNAL, SEPTEMBER, 1955

NITROGEN EFFECTS IN ARGON ARC WELDING ATMOSPHERES

Laboratory investigation proves need for high argon purity in the application of this gas to the shielding of steel welding arcs

BY H. C. LUDWIG

ABSTRACT. The solubility of nitrogen in steel arc weld metal has been determined when using argon-nitrogen atmospheres. The results indicate greater chemical activity than that expressed by a square root law of gas solubility with respect to nitrogen pressure in the arc welding atmosphere. The solubility of nitrogen at one atmosphere was found to be in the range of 0.038–0.041% by weight. The average found corroborates the accepted value of nitrogen solubility in pure iron at 1540° C under equilibrium conditions.

Not all of the nitrogen absorbed by the molten weld metal is retained. As the metal cools to the solidus, nitrogen is released to form gross porosity. The porosity has been found detrimental above about 1% by volume nitrogen in the argon arc welding atmosphere. These data explain the reasons for the high argon purity requirement in the application of that gas to the shielding of steel welding arcs.

H. C. Ludwig is associated with the Welding Section, Research Laboratories, Westinghouse Electric Corp., East Pittsburgh, Pa.

Presented at AWS National Spring Meeting held in Kansas City, Mo., June 7–10, 1955.

Introduction

The nitrogen content of argon shielding gas is critical in the arc welding of low carbon mild steel. A nitrogen absorption-desorption mechanism in molten steel and iron causes weld porosity. This paper describes the effects of nitrogen in argon atmospheres on consumable mild steel electrode arc welding deposits and provides the tolerance limit of nitrogen contents in the shielding gas.

The solubility of nitrogen in iron has been extensively investigated because of its influence in the making and working of steel. However, present-day knowledge of nitrogen solubility in the iron and steel liquid phase remains limited, particularly with respect to the presence of nitrogen in gas mixtures. Sieverts,[1] Chipman,[2] Sawyer[3] and others have determined the nitrogen solubility in the iron liquid phase for pure nitrogen atmospheres. Chipman found that pure iron maintained at 1540° C under one atmosphere of pure nitrogen would contain 0.039% nitrogen by weight with an increase of 1.5×10^{-4}%/° C to 1760° C. Sawyer claimed that his results follow a square root law with nitrogen pressure with 0.0158% N dissolved at one atmosphere at the melting point of iron. Sieverts' value of nitrogen solubility in pure iron at one atmosphere at 1540° C is 0.031% N. Brick and Creevy[4] obtained 0.038% in pure iron at one atmosphere at approximately 1590° C. Fast[5] concluded that nitrogen diffuses too slowly from liquid iron for the amount of nitrogen retained to be altered appreciably by the rate of cooling. The Sievert-Chipman values for nitrogen solubility in iron at one atmosphere pressure for a range of temperature are shown graphically in Fig. 1.

Hensel[6] found that the nitrogen content of weld metal was reduced considerably in a nitrogen arc welding atmosphere as compared to arc welding in air atmosphere. Nitrogen may be absorbed in bare wire arc welds in an air atmosphere to the extent of 0.15–0.20% by weight. Nitrogen absorption in

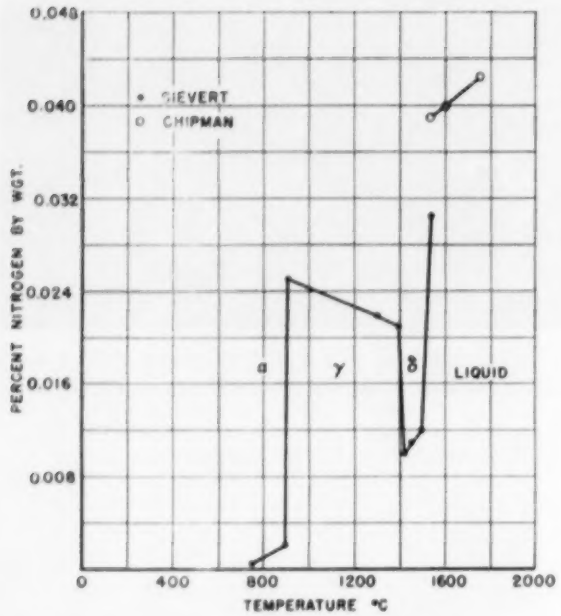

Fig. 1 Solubility of nitrogen in iron at 760 mm Hg

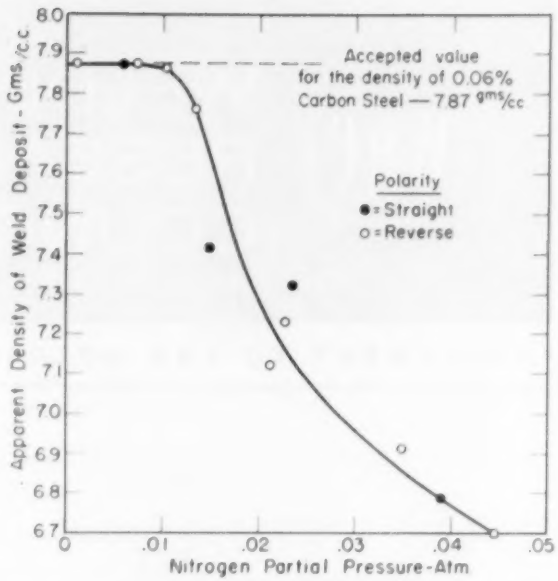

Fig. 2 Apparent density of weld deposits in argon-nitrogen arc welding atmospheres

Table 1

Nitrogen partial pressure arc atmosphere, atm	*Density weld deposit, gm/cc*	*Combined nitrogen weld deposit, % wt*	*1/Cube root partial pressure N_2, $1/atm^{1/3}$*	*Partial pressure N_2/% N weld deposit, atm/% N*	*Arc voltage, v*	*Arc current, amp*	*Polarity (Electrode)*	*Test No.*
0.0009	..	0.0004	10.36	2.25	21	190	+	41 AN
0.0012	7.87	..	..	..	23	205	+	104 A
0.0074	7.88	0.012	5.13	0.62	19	215	+	9 AN
0.0102	7.86	0.011	4.61	0.93	23	200	+	8 AN
0.0134	7.76	0.014	4.21	0.96	20	210	+	10 AN
0.0212	7.12	0.024	3.61	0.88	23	200	+	33 AN
0.0226	7.23	0.023	3.53	0.98	21	210	+	15 AN
0.0328	6.92	0.017	3.12	1.93	22	210	+	11 AN
0.0445	6.70	0.024	2.82	1.86	21	200	+	14 AN
0.109	..	0.033	2.09	3.21	23	200	+	35 AN
0.177	..	0.035	1.78	5.06	26	190	+	36 AN
0.324	..	0.040	1.45	8.10	27	185	+	37 AN
0.512	..	0.040	1.25	12.8	28	190	+	38 AN
0.798	..	0.041	1.08	19.5	29	175	+	39 AN
0.999	..	0.041	1.00	24.6	30	175	+	40 AN
0.0087	..	0.0015	4.85	5.80	23	330	+	55 AN
0.179	..	0.031	1.77	5.80	26	320	+	61 AN
0.999	..	0.038	1.00	26.3	32	300	+	56 AN
0.0049	7.61	0.007	5.88	0.70	26	170	−	18 AN
0.0059	7.87	0.012	5.68	0.43	26	170	−	19 AN
0.0148	7.41	0.015	4.06	0.87	26	170	−	20 AN
0.0168	7.44	0.019	3.90	0.88	26	170	−	30 AN
0.0236	7.32	0.022	3.49	1.07	26	170	−	31 AN
0.0297	6.68	0.024	3.23	1.24	26	170	−	32 AN
0.0388	6.79	0.028	2.95	1.38	26	170	−	22 AN
0.0845	3.98	0.030	2.28	2.82	26	170	−	23 AN
0.1405	..	0.032	1.92	4.38	26	170	−	24 AN
0.228	..	0.034	1.64	6.70	26	170	−	25 AN
0.345	..	0.037	1.42	9.33	26	170	−	26 AN
0.572	..	0.038	1.21	15.1	26	170	−	27 AN
0.861	..	0.039	1.05	22.1	32	150	−	28 AN
0.999	..	0.039	1.00	25.6	32	150	−	29 AN
0.0037	..	0.003	6.46	1.23	28	330	−	59 AN
0.160	..	0.034	1.84	4.71	27	340	−	60 AN
0.999	..	0.041	1.00	24.4	32	325	−	58 AN

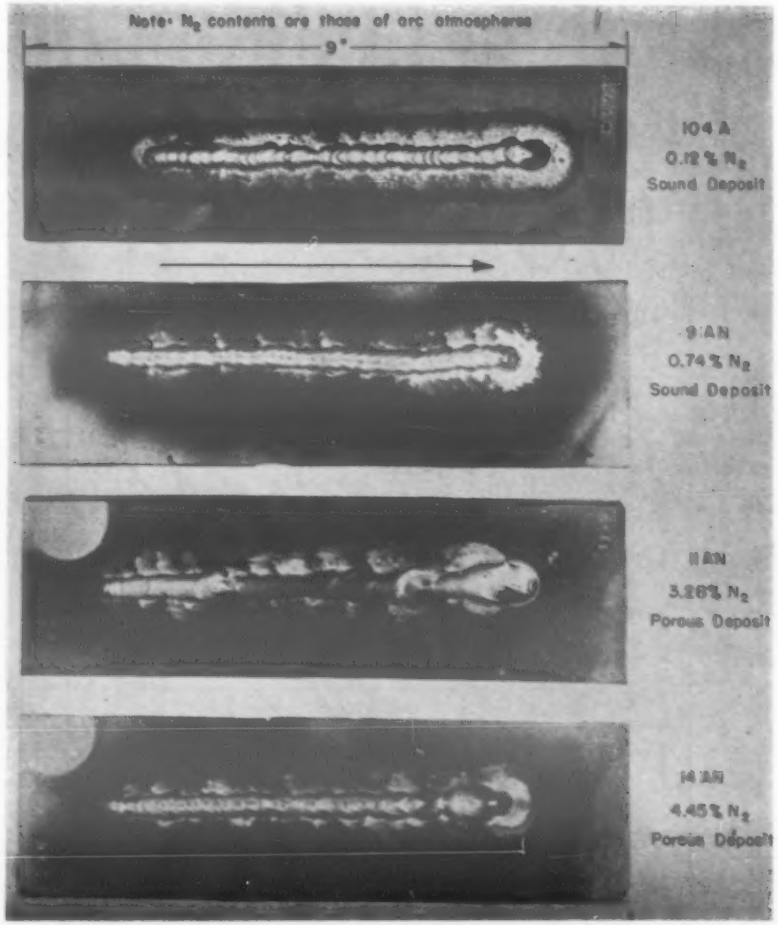

Fig. 3 Weld deposits in argon-nitrogen atmospheres

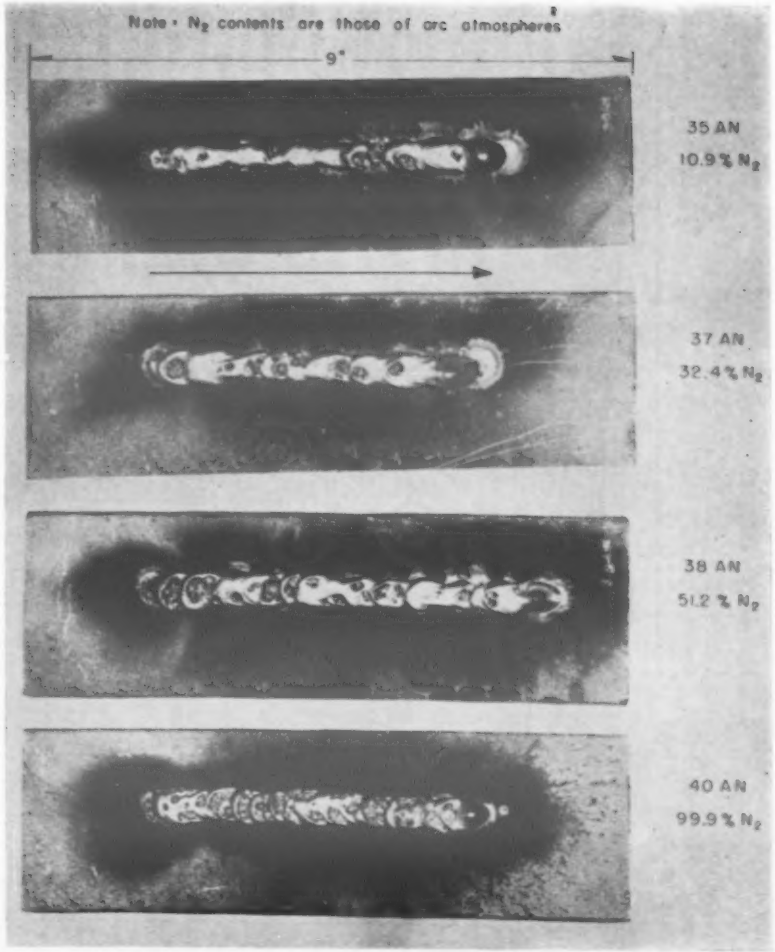

Fig. 4 Weld deposits in argon-nitrogen atmospheres

oxy-acetylene welding of low-carbon mild steel seldom exceeds 0.02% by weight, apparently due to the shielding action of the acetylene and oxygen or their combustion products.

Argon commercially extracted from natural atmosphere by liquid air fractionation and supplementary chemical purification processes is produced to a purity of about 99.6–99.9%, the major impurity being nitrogen. One objective of the work described here was to determine the effect of residual quantities of nitrogen as an impurity in argon shielding gas on mild steel arc welding deposits.

	%						
Material	*C*	*Mn*	*P*	*S*	*Si*	*O*	*N*
Electrode	0.18	0.55	0.011	0.039	0.09	0.0076	0.0044
Plate workpiece	0.16	0.54	0.022	0.031	0.07	0.0115	0.0041

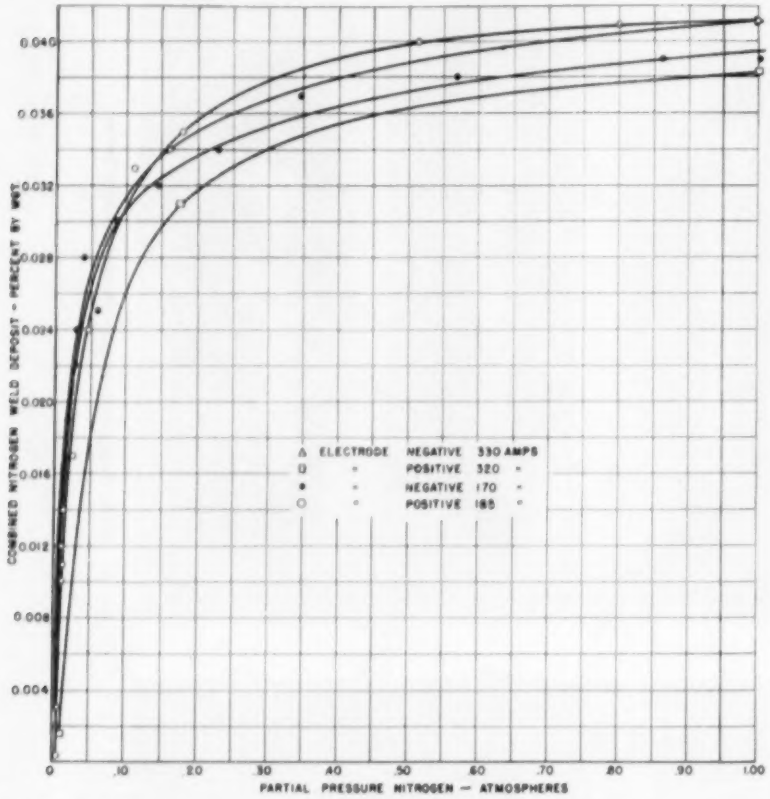
COMBINED NITROGEN WELD DEPOSIT - PERCENT BY WGT
PARTIAL PRESSURE NITROGEN — ATMOSPHERES
Δ ELECTRODE NEGATIVE 330 AMPS
□ " POSITIVE 320 "
• " NEGATIVE 170 "
○ " POSITIVE 185 "

Fig. 5 Combined nitrogen in weld deposits of argon-nitrogen arc atmospheres

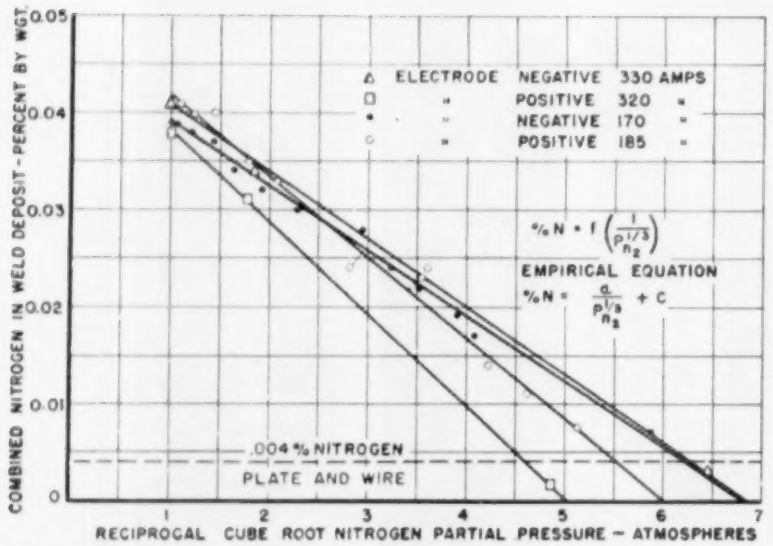
COMBINED NITROGEN IN WELD DEPOSIT - PERCENT BY WGT
RECIPROCAL CUBE ROOT NITROGEN PARTIAL PRESSURE — ATMOSPHERES
Δ ELECTRODE NEGATIVE 330 AMPS
□ " POSITIVE 320 "
• " NEGATIVE 170 "
○ " POSITIVE 185 "
%N = f (1/P^1/3 n2)
EMPIRICAL EQUATION
%N = a/P^1/3 n2 + C
.004 % NITROGEN
PLATE AND WIRE

Fig. 6 Nitrogen solubility in mild low-carbon steel argon-nitrogen arc atmospheres

Test Program and Results

Rigid control of the arc shielding gas was assured by making weld deposits in a chamber specifically designed to maintain the desired gas compositions. The high purity gases which formed the argon-nitrogen arc atmospheres were mixed before entering the chamber. Proportions of each gas were regulated by individual calibrated manometers fed to a simple gas mixing unit. A purging technique was employed to displace the existing gas in the chamber with the desired gas mixture. A total gas pressure of about 760 mm Hg was maintained for the tests. A flow of the gas mixture was maintained during each test. Gas samples were taken directly from the chamber and analyzed by mass spectrometric means. The nitrogen content of the arc atmosphere was varied from 0.09 to 99.9% in controlled additions, the other component being argon.

The consumable electrode and workpiece materials were low-carbon mild steel of compositions:

Direct current supplied by a constant potential generator was used operating at 70 v open circuit. Welds were made with electrode both positive and negative in two current ranges. The electrode and plate material were precleaned to remove surface scale or other foreign materials.

Sampling of the weld deposits for nitrogen analysis was carried out by milling to 1/16 in. of the plate (workpiece) surface. The chemical analysis for nitrogen retained in the weld deposits was made by a Kjeldahl distillation method. Where practical, density measurements of the weld deposits were made. The density measurements included only cases of low nitrogen partial pressures in the argon-nitrogen welding atmospheres.

In general, nitrogen in the argon-nitrogen arc welding atmospheres is absorbed by the molten weld metal and then partially desorbed as the metal cools, which may cause porosity. A measure of the desorbing action or evolution of nitrogen is shown by the graph of Fig. 2, where the apparent density of

the weld deposit (the weight of the deposit divided by its volume) is plotted against the partial pressure of nitrogen in the welding atmosphere. The effect of desorption of nitrogen in the weld deposits is illustrated by the photographs of Figs. 3 and 4 which show an increase in porosity with increases in partial pressure of nitrogen in the arc welding atmospheres. No appreciable change in apparent density occurs until more than about 0.01 atmosphere partial pressure of nitrogen (approximately 1%) is used, Fig. 2.

The relationship of retained nitrogen and the nitrogen partial pressure in the atmosphere was correlated with the density measurements. The solubility of nitrogen in the weld deposits as influenced by the partial pressures of nitrogen in the arc atmosphere is shown by the curves of Fig. 5. A family of curves was secured with a variation in arc current and electrode polarity; however, there were no essential differences in the types of curves obtained. The solubility of nitrogen is roughly proportional to the square root of nitrogen partial pressure at small pressures, but falls off approaching higher pressures as the solubility value becomes asymptotic to approximately 0.05% N by weight. The nitrogen solubility data are shown in Table 1, which also contains density measurements on the weld deposits and calculations for the data treatment to be described.

In a treatment of the data from the graphs of Fig. 5 and Table 1, a suitable form of an empirical equation was found involving the reciprocal cube root of nitrogen partial pressure:

$$\% \text{ N} = \frac{a}{P_{N_2}^{1/3}} + C \qquad (1)$$

where % N is the nitrogen by weight in the weld deposit; P_{N_2} is the partial pressure of nitrogen in the arc atmosphere; a and C are constants; a being the slope with negative values and C the intercept on the ordinate when plotting % N vs. $1/P_{N_2}^{1/3}$. This plot results in a series of straight lines for the entire experimental range, including combined nitrogen contents below the initial amount of the electrode and plate material, Fig. 6. The extrapolated value on the intercept of the ordinate for each curve of Fig. 6 is the constant C which corresponds to an infinite pressure of nitrogen in the atmosphere if the same relations would hold to that condition. The intercepts on the abscissa correspond to the partial pressures of nitrogen where no combined nitrogen would remain in the weld metal.

A linear relationship was also found between the ratio of partial pressure of nitrogen in the atmosphere to the combined nitrogen of weld metal and the nitrogen partial pressure except in the range at very low nitrogen partial pressures, Figs. 7 and 8. The data fit the empirical equation:

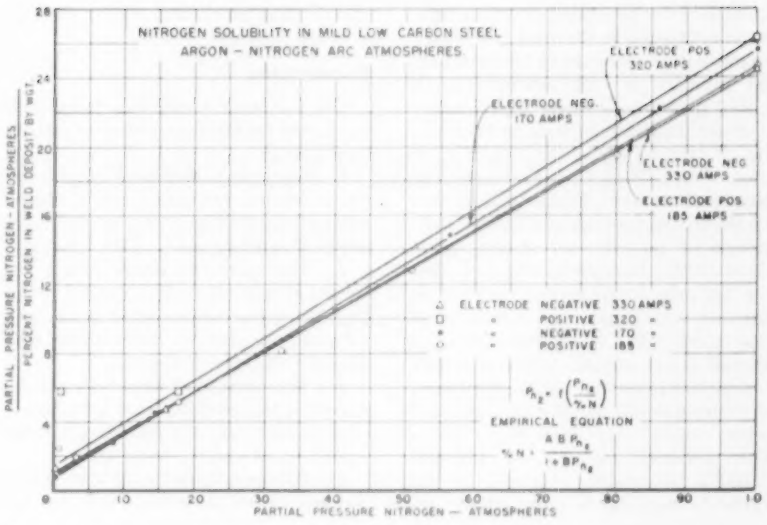
NITROGEN SOLUBILITY IN MILD LOW CARBON STEEL
ARGON – NITROGEN ARC ATMOSPHERES
ELECTRODE POS. 320 AMPS
ELECTRODE NEG. 170 AMPS
ELECTRODE NEG. 330 AMPS
ELECTRODE POS. 185 AMPS
ELECTRODE NEGATIVE 330 AMPS
POSITIVE 320 "
NEGATIVE 170 "
POSITIVE 185 "
EMPIRICAL EQUATION
PARTIAL PRESSURE NITROGEN – ATMOSPHERES
PERCENT NITROGEN IN WELD DEPOSIT BY WGT
PARTIAL PRESSURE NITROGEN – ATMOSPHERES

Fig. 7 Nitrogen solubility in mild low carbon steel argon-nitrogen arc atmospheres

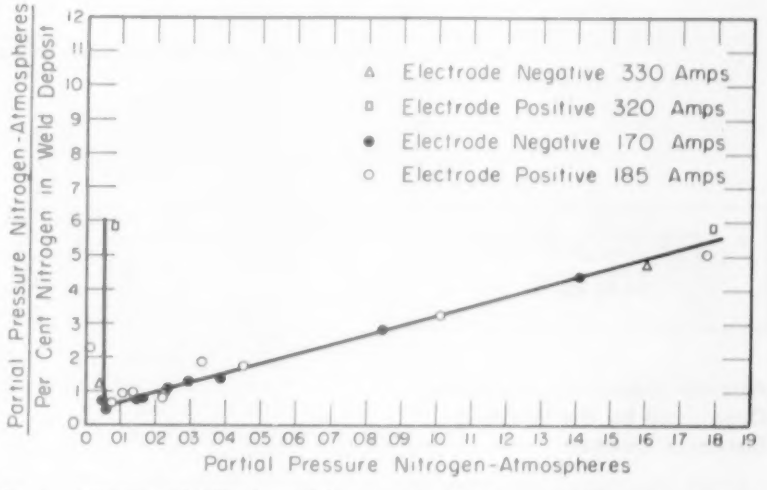
Electrode Negative 330 Amps
Electrode Positive 320 Amps
Electrode Negative 170 Amps
Electrode Positive 185 Amps
Partial Pressure Nitrogen-Atmospheres Per Cent Nitrogen in Weld Deposit
Partial Pressure Nitrogen-Atmospheres

Fig. 8 Nitrogen solubility in mild low-carbon steel in argon-nitrogen arc atmospheres (supplementary to Fig. 7)

$$\% \text{ N} = \frac{ABP_{N_2}}{1 + BP_{N_2}} \qquad (2)$$

where A and B are constants.

Equation 2 can be rearranged to:

$$\frac{P_{N_2}}{\% \text{ N}} = \frac{P_{N_2}}{A} + \frac{1}{AB} \qquad (3)$$

Equations 2 and 3 do not conform to the data below values of nitrogen partial pressure about 0.007 atmosphere as shown in Fig. 8. These points correspond to the amount of combined nitrogen originally present in the electrode and plate, that is, 0.0044% N, wherein the $P_{N_2}/\%$ N values increase again with a decrease in nitrogen partial pressure. In general, nitrogen is absorbed and partially desorbed by the weld metal for partial pressures down to a range of about 0.004–0.01 atmosphere nitrogen. This corresponds to 0.4–1.0 mole or volume percent nitrogen in argon, assuming ideal gas behavior. A correlation appears to exist between the density measurements obtained and the anomalous behavior found at low partial pressures of nitrogen since a change in apparent density of the deposited metal occurs at the same range of partial pressures of nitrogen. In Fig. 8, the straight line portions of the curves fitting Eqs. 2 and 3 represent partial pressures of nitrogen in the welding atmosphere for which porosity in the weld deposits is evident. Porosity was not evident at partial pressures of nitrogen in the arc weld atmosphere where a straight line relationship of negative slope was obtained. This range of nitrogen partial pressure is further defined as where the ratio of partial pressure of

nitrogen in the arc atmosphere to weight percent nitrogen in the weld deposit increases with a decrease in nitrogen partial pressure in the atmosphere. A critical pressure is evident which occurs at about 0.007 atmosphere as shown in Fig. 8 where $[d(P_{N_2}/\%\ N)]/dP_{N_2}$ is zero. From Fig. 2 where apparent density of the weld metal is plotted versus the partial pressure of nitrogen, indications of porosity are shown above about 0.007 atmosphere nitrogen. Below this value sound deposits in all cases but one was found. It appears then that about 0.007 atmosphere nitrogen represents a critical pressure above which absorption and partial desorption of nitrogen occurs in the weld metal and below which only desorption occurs. The residual amount of nitrogen (0.004% by weight) originally in the electrode material is substantially reduced, thus producing weld metal of very low nitrogen content.

Conclusions

The following conclusions are drawn supported by the data obtained and its treatment:

1. Nitrogen is dissolved by low-carbon mild steel in welding arcs of argon-nitrogen atmospheres and partially retained on cooling.

2. The value of about 0.04% by weight nitrogen absorbed by steel at the liquidus temperature appears to corroborate the findings of Chipman and Murphy, Brick and Creevy.

3. The amount of fixed nitrogen retained in the weld deposits described is altered only slightly by the arc heat energy input and its distribution.

4. Nitrogen initially present in the welding electrode material can be significantly reduced by restricting the nitrogen to below a partial pressure of 0.005 atmosphere in the argon arc gas shield.

5. From the practical point of view, argon used in inert-gas-shielded consumable-electrode processes should contain no more than 1.0% nitrogen and preferably less than 0.5%. The nitrogen may be an impurity of the cylinder argon and/or nitrogen may be introduced from the natural atmosphere which is in proximity to the argon gas shield.

References

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3. Sawyer, C. B., *Ibid.*, **69**, pp. 798–828 (1923).
4. Brick, R. M., and Creevy, J. A., *Ibid.*, Tech. Paper 1165, Vol. 7 (1940).
5. Fast, J. D., *Philips Tech. Rev.*, No. 4, Vol. II (October 1949).
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RESEARCH NEWS

Aircraft Advisory Committee Organized by the Welding Research Council

At the suggestion of several sources, the Welding Research Council organized an Aircraft Advisory Committee, with L. M. Crawford as its chairman. Serving on this Advisory Committee are representatives from the interested governmental agencies and important aircraft companies. The purpose of this Aircraft Advisory Committee was to review the needs of the aircraft industry and make basic recommendations to the Welding Research Council and to other interested agencies in regard to needed research work relating to aircraft, airframe and engine manufacture. Accordingly, the Welding Research Council canvassed the aircraft industry and from various sources obtained a list of some 30 problems which the aircraft industry thought merited consideration by the Aircraft Advisory Committee.

The Aircraft Advisory Committee carefully considered these various problems and selected five problems as having the greatest interest to most of the organizations in the industry. The five problems selected are:

1. Development of Filler Wire and Covered Electrodes for Welding Heat Treatable, Low-Alloy, High-Tensile Steels
2. Fusion Weldability of High Strength Aluminum Alloys 2014, 2024 and 7075
3. Investigation of Weldable Grades of Precipitation Hardening Type Corrosion Resistant Alloys for Normal Temperature Use
4. Development of a Weldable Precipitation Hardening Alloy for Use in the Range 1200–1350° F
5. Resistance Welding Control Systems
   (*a*) Development of a resistance weld control system which will produce a realistic and acceptable uniformity in the quality of production spot and roll welds
   (*b*) Development of a reliable nondestructive method for inspection of flash welds

These problems were formulated by the committee and have been distributed in pamphlet form to the Armed Services, the Aircraft Industries Association and to appropriate committees of the Council. Undoubtedly, interested groups will undertake the necessary research work and eventually the results will be made available to industry.

WRC Plasticity Committee Makes Headway

The Welding Research Council Plasticity Committee, under the chairmanship of Paul F. Ffield, is making considerable headway in the final preparation of a manual on *Designing to Prevent Brittle Failure of Field-Erected Welded Steel Structures*. The manual is to be divided into three parts, Materials, Fabrication Procedures and Design. There will also be included a Glossary of Terms and Test Specimens referred to in the manual.

Several drafts have been prepared of each section. Comments have been secured from a representative cross section of industry for each of the parts. An editor has been selected and assigned the first task of coordination of the three parts, elimination of duplication and rearrangement of the material in a logical, useful fashion. After this task is completed the first draft of the edited copy will again be resubmitted to a number of people for comments and the manual will then be printed.

Every effort has been made by the Plasticity Committee to provide material which the average designing engineer can use. Compromises have been effected on many of the controversial points. For the most part, these compromises represent a realistic appraisal from an economical viewpoint of what can be done now with presently available materials.

STRESS-RELAXATION AND STRESS-RELIEF OF SOME MAGNESIUM ALLOYS

Stress-relaxation measurements by the use of the simple and cantilever beam are described for several commercial magnesium alloys. Requirements of stress relief of welded parts are pointed out

BY PAUL KLAIN

ABSTRACT. The stress-relaxation conditions for several commercial magnesium alloys by the use of the simple beam and cantilever beam methods are presented. Stress-relaxation curves for AZ31A sheet, M1A sheet, ZK60A extrusion and AZ63A, AZ92A and ZK51A castings at several different temperatures and initial stress levels are included and the significant trends discussed. The requirements for stress relief of welded and of machined parts are pointed out.

Introduction

Stress-relief treatments are widely used on many metals to remove internal or "locked-in" stresses. The treatments are most commonly applied to metals after welding or machining. Internal stresses may or may not be harmful and their removal may not always be necessary. In fact, stresses may be deliberately induced in some parts to improve their performance, as in the case of peened parts in which fatigue life is greatly improved by the presence of compressive surface stresses. In the magnesium industry, stress relieving is widely used on most alloys after welding and is sometimes used on unwelded castings. However, it is not common practice to stress-relieve castings unless welded or unless close dimensional tolerances after machining are required.

The removal of internal stresses is most easily accomplished by heating the parts to a sufficiently high temperature to allow plastic flow to occur. This results in a phenomenon known as stress relaxation. Relaxation is a form of creep occurring under a diminishing stress, as differentiated from the usual creep phenomenon, which takes place under a constant stress. In heating the parts for relaxation to occur, the temperature and the time of heating must be controlled so that the properties of the materials are not affected adversely. This is especially true of the strain or precipitation-hardened metals.

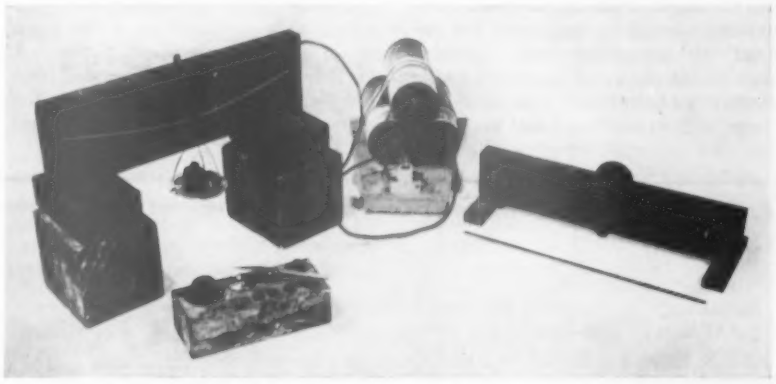

Fig. 1 Equipment for simple beam relaxation tests

The internal or residual stresses whose removal may be desirable are those originating from any nonuniform plastic deformation such as strain hardening or cooling effects. Stresses originating from strain hardening may be caused by bending, drawing, twisting, machining, grinding, peening, etc. Stresses arising from cooling effects may be associated with casting, quenching, cooling, welding and metallurgical phase changes.

Until recently the consideration of relief of residual stresses in magnesium alloys has received scant attention in the literature. The work of R. J. M. Payne[2] is the only published information containing factual data regarding stress-relief conditions and their effect on properties. Payne's studies covered the stress-relief treatments for the British casting alloys AZ91 (9Al – 1Zn), A8 (8Al), Z5Z (4.5Zn – 0.7Zr) and MCZ (3mischmetal – 0.6Zr). Found and Pittsley[3] devised techniques for measurement of residual stresses and determined their magnitude in AZ63A and AZ92A commercial castings. They found that residual stresses in magnesium castings were considerably lower than generally found in other alloys and that aging treatments essentially eliminated them. Hildebrand and Strieter[4] reported that ZK51A castings required a 2-hr heat treatment at 600° F for 95% stress relief. This was in good agreement with Payne's results on the equivalent British Z5Z alloy.

Stress-Relaxation Measurements

Two methods were used to determine the degree of stress relaxation. Initially the simple beam of uniform cross section but of varying stress as suggested by Kempf and VanHorn[1] was used on sheet materials. These investigators

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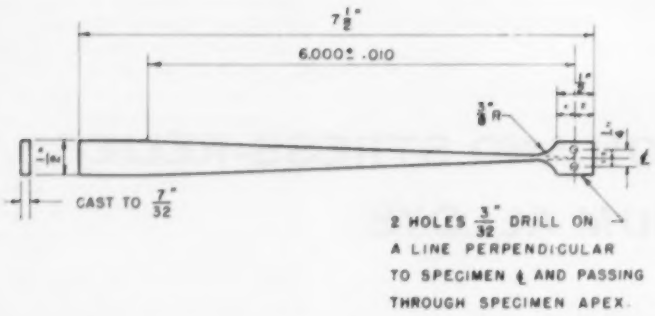

Fig. 2 Dimensions of the cantilever beam specimen used for relaxation tests

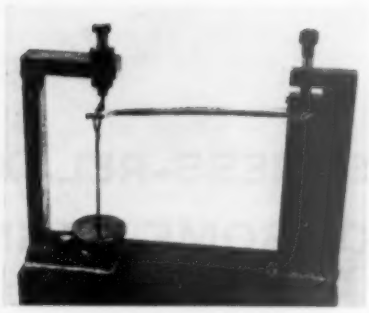

Fig. 3 Jig for cast cantilever relaxation specimen with weight pan and specimen

showed that the method correlated closely with Sach's determinations of residual stresses in solid cylinders. Later, the uniformly stressed cantilever beam specimen (Fig. 2) was developed because of certain difficulties involved in the preparation and use of cast specimens in the simple beam test. The cantilever beam specimen was about half the length of the simple beam, thus allowing casting to shape and size instead of machining from castings. Since its inception, the cantilever beam method has been found satisfactory for use in both wrought and cast materials and has completely replaced the simple beam method. Both methods will be fully described.

1. Simple Beam Procedure

The equipment for the simple beam relaxation tests is shown in Fig. 1. The specimen was prepared from 0.064-in. sheet, machined to 0.250 in. width and was 12 in. in length. The specimen was placed across the two outside steel pins which were insulated from the main frame of the relaxation fixture. A loaded weight pan was suspended from the center of the beam to produce the desired fiber stress. The center pin was adjusted to contact the deflected beam and then clamped in position. The specimen was carefully removed and the jig preheated to the desired temperature. The specimen was replaced in the jig and both were heated for a predetermined time, after which the specimen was quickly removed from the jig and both allowed to cool separately. After both were cool, the specimen was reinserted into the jig and the weight required to break contact from the pin was determined. The difference between the original load and the load after heating was the relaxation load.

Table 1—Composition and Mechanical Properties of Some Commercial Magnesium Alloys

Alloy designation, ASTM	*Alloy condition**	*%*					*Typical properties*			
		Al	*Zn*	*Mn*	*Zr*	*Mg*	*1000 psi* *TS*	*TYS†*	*CYS*	*% elongation in 2 in.*
				Sheet materials						
M1A	-O	..	..	1.2	..	Bal.	33	18	12	16
	-H24						37	28	24	7
AZ31A	-O	3.0	1.0	0.3	..	Bal.	37	22	16	21
	-H24						42	32	27	16
				Extrusions						
M1A	-F	..	..	1.2	..	Bal.	34	20	12	9
AZ31B	-F	3.0	1.0	0.3	..	Bal.	37	26	15	12
AZ61A	-F	6.5	1.0	0.2	..	Bal.	44	30	19	14
AZ80A	-F	8.5	0.5	0.2	..	Bal.	48	32	..	12
AZ80A	-T5						52	36	33	5
ZK60A	-F	5.7	..	..	0.55	Bal.	49	38	33	12
ZK60A	-T5						51	43	34	10
				Castings						
AZ63A	-F	6.0	3.0	0.15	..	..	29	14	14	6
	-T4						40	14	14	12
	-T5						29	14	..	5
	-T6						40	19	19	5
AZ92A	-F	9.0	2.0	0.15	..	..	24	14	14	2
	-T4						40	14	14	10
	-T5						24	14	..	2
	-T6						40	23	23	2
AZ91C	-F	8.7	0.7	0.13	..	..	24	14	14	2
	-T4						40	14	14	11
	-T6						40	19	19	4
ZK51A	-F	4.6	..	..	0.75	..	38	21	..	9
	-T5						41	26	26	8

* -F = As-cast, as-rolled or as-extruded; -T4 = solution heat treated; -T5 = artificially aged; -T6 = solution heat treated and artificially aged; -H24 = hard rolled or strain hardened; -O = Annealed (wrought products)

† Stress at which stress-strain curve deviates 0.2% from modulus line.

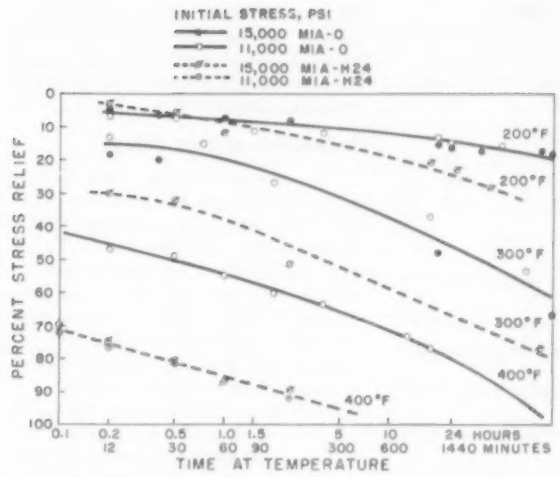

Fig. 4 Relaxation of M1A sheet in the -0 and -H24 tempers

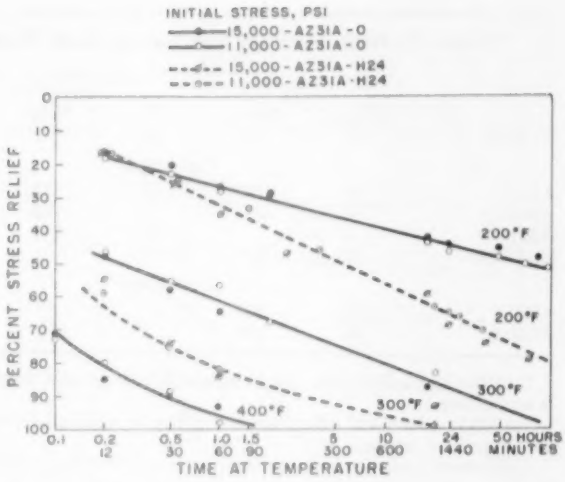

Fig. 5 Relaxation of AZ31A sheet in the -0 and -H24 tempers

This was then converted to fiber stress. Duplicate tests showed reproducibility of ±200 psi.

2. *Cantilever Beam Procedure*

The cantilever beam specimen shape and size are given in Fig. 2. The thickness of the specimen could be varied from 0.064 to 0.2125 in., while the maximum width was fixed at $^1/_2$ in. and the length was 6 in. from the point of restraint to the loading point. The equipment used is shown in Fig. 3. The specimen was deflected to the desired initial stress by weights in the suspended pan and the setscrew in the jig was adjusted to maintain the deflection. As in the simple beam procedure, the specimen was removed and the fixture preheated separately, then the specimen was reinserted and both reheated to the desired conditions. After the specimen was cooled separately from the jig, the load required to break contact between the specimen and the setscrew in the fixture was determined. The difference between the initial and the final load was the relaxation load, which was then calculated to stress values by the uniformly stressed cantilever beam formula: $S = 6PL/bd^2$, where S was the stress in psi, P the load, L the specimen length, b the specimen width and d the specimen thickness.

Before each test at a given temperature, a blank relaxation specimen, with a thermocouple, was placed in the furnace to determine the specimen temperature and the time required to bring the specimen up to temperature. The heating-up time was deducted from short holding times but was disregarded for the longer heating times.

Materials

The commercial magnesium alloys for which stress-relaxation data were obtained are listed in Table 1 according to ASTM designations. Also included are some alloys which had not been studied but for which stress-relief recommendations were available. The heat treating schedules for some of these alloys are given in Table 2 for comparison with stress relief conditions established in this investigation.

Presentation of Data

The relaxation data were plotted as percent stress relief against the time of heating on a semilogarithmic scale. These charts were believed to be of more practical value to the engineer than the stress-time type. The tabular data were not included since the necessary points were plotted in the graphs. For weldments, a minimum value of 80% relaxation was found to give satisfactory stress relief.

Wrought Alloys

The relaxation data for the sheet alloys M1A and AZ31A are shown in Figs. 4 and 5. These values were obtained by the use of the simple beam method. The relaxations were obtained at temperatures of 200, 300 and 400° F for initial stresses of 11,000 and 15,000 psi. The curves showed that 80% stress relief was obtained by heating M1A-H24 for $^1/_2$ hr and the M1A-0 for 24 hr at

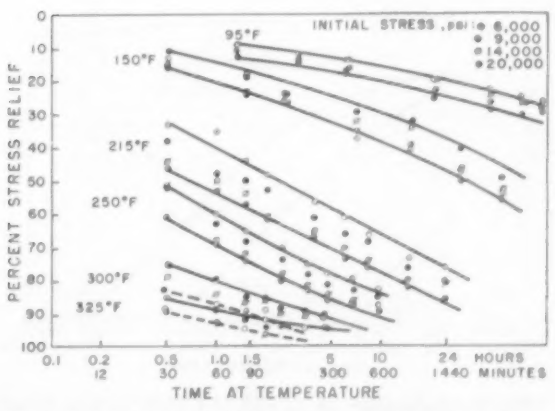

Fig. 6 Relaxation of ZK60A -F from various initial stresses

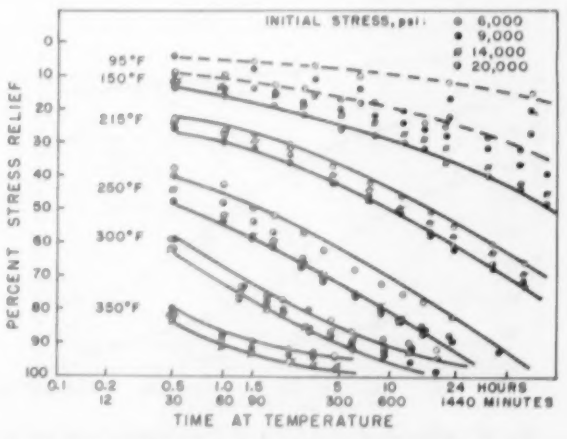

Fig. 7 Relaxation of ZK60A -T5 from various initial stresses

Table 2—Heat Treatment Schedules for Some Magnesium Alloys*

Alloy	Treatment —T5— Time, hr	—T5— Temp, ° F	—T4† Time, hr	—T4† Temp, ° F	—T6 Time, hr	—T6 Temp, ° F
Cast alloys						
AZ63A	4	500	12	725	5	425
AZ92A‡	4	500	18	765	4	500
AZ91C‡	..	..	18	775	16	335
ZK51XA	12	350	..	..	..	..
Extrusions						
AZ80A	18	350				
ZK60A	24	300				

* AZ92A and AZ91C may require special heat-treatment conditions where germination is encountered.

† AZ63A, AZ92A and AZ91C are brought up to holding temperatures at a uniform slow rate to prevent melting.

‡ Alternative solution heat treatments are available if required to prevent germination.

400° F. Commercial practice has been to stress relieve the M1A-H24 for 1 hr at 400° F and the M1A-0 for 15 min at 500° F. As the results indicated there seemed to be some basis for the commercially used stress-relief conditions for M1A-0 material even though no actual data existed. It was assumed that the curve for the 500° F heating tests would fall below the 400° F curve for the -H24 temper, judging by the relationships of the curves obtained at 300° F.

The curves for AZ31A sheet in Fig. 5 showed 80% stress relief was obtained after 1 hr at 300° F for the -H24 temper and approximately 10 hr for the -0 temper. By increasing the temperature to 400° F, about 95% stress relief was obtained after 1 hr. The commercial stress-relief treatments, based on adherence to single conditions for all alloys, required 1 hr at 300° F for the AZ31A-H24 and 15 min at 500° F for the AZ31A-0 sheet. These stress-relief conditions did not affect the original properties of the materials.

A comparison of the data for the two alloys showed that AZ31A had a higher rate of relaxation than M1A. In both materials, the -H24 temper resulted in faster relaxation than the -0 temper, in agreement with the known fact that recovery of work-hardened materials tends to increase stress-relief rate. The plotted data also showed that the percent stress relief was independent of the initial stress.

The data for the extrusion alloy ZK60A as well as for all the other data that follow were obtained by the use of the cantilever beam method. The thickness of ZK60A specimens was kept at 0.080 in. with as-extruded stressed surfaces. Initial stresses of 6000, 9000, 14,000 and 20,000 psi were used. The resulting data appear in Figs. 6 and 7 as bands rather than single-line curves because of the scatter. The as-extruded (-F) alloy relaxed at a faster rate than the aged. A heating temperature of 300° F resulted in 80% stress relief after 1 hr and over 95% after 5 hr. For the -T5 temper, a temperature of 350° F was required to give similar results. However, to prevent loss of room temperature properties, the ZK60A-T5 should not be heated over 300° F. Commercial stress-relief, therefore, has been limited to heating at 300° F for 1 hr, which results in approximately 80% stress relief. The extruded or -F temper can be safely heated to 500° F without loss in properties and therefore commercial stress-relief conditions have been fixed at 500° F for 15 min.

Relaxation of Cast Alloys

The stress-relaxation data for the alloys AZ63A and AZ92A in the -F, -T4 and -T6 conditions are shown plotted in Figs. 8–15. For the -F and -T4 tempers, the effects of heating temperatures of 200, 350, 425, and 500° F on initial stresses of 4000, 5000, 7000 and 10,000 psi were investigated. For the -T6 temper, the 425° F temperature and the 5000 psi stress were omitted.

The stress-relief curves for the AZ63A alloy for each of the tempers are given in Figs. 8–10 while the significant curves for all three tempers are given in Fig. 11 for comparison. The points used in plotting the curves of Fig. 11 were picked from the drawn curves of Figs. 8–10. The relaxation data for the different tempers were best illustrated by the combined curves in Fig. 11. The highest relaxation rate was exhibited by the alloy in the -T4 temper at all temperatures, although at 350 and 425° F, this occurred only after a considerable holding time. At 500° F, more than 90% stress relief was obtained after 1 hr at temperature and very little difference in the shape of the curves was evident. At the lower temperatures, which also were the artificial aging temperatures, the greatest deviation from a straight line relationship occurred with the -T4 temper and the least with the -T6, which gave almost a straight line. The significance of this will be discussed later.

The curves for the AZ92A alloy in the three tempers -F, -T4 and -T6, are shown in Figs. 12–14, while the combined data, plotted with points obtained from these curves, are shown in Fig. 15. Relaxation curves showed trends similar to those observed with AZ63A. The -T6

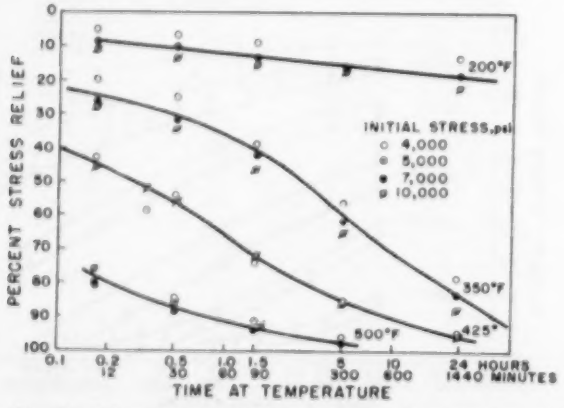

Fig. 8 Relaxation of AZ63A -F from various stresses at different temperatures

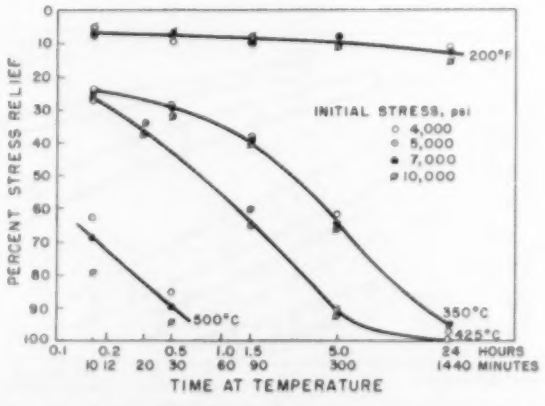

Fig. 9 Relaxation of AZ63A -T4 from various initial stresses at different temperatures

temper showed almost a straight line relationship, while increasing deviations occurred in the -F and -T4 tempers. The latter resulted in the highest rate of relaxation after an initially slow rate. As with AZ63A, the most practical temperature for stress-relief treatments was found to be 500° F for holding times up to 1 hr.

A comparison of the rates of relaxation of the two alloys in each of the tempers at temperatures of 350, 425 and 500° F are shown in Figs. 16–18. In the -F temper, practically no significant difference was noted, as was evident from the curves in Fig. 16. In the -T4 and the -T6 condition, the relaxation rate of the AZ92A was somewhat higher

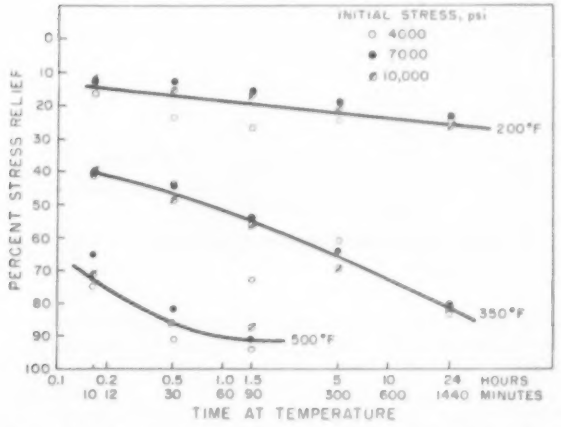

Fig. 10 Relaxation of AZ63A -T6 from various initial stresses at different temperatures

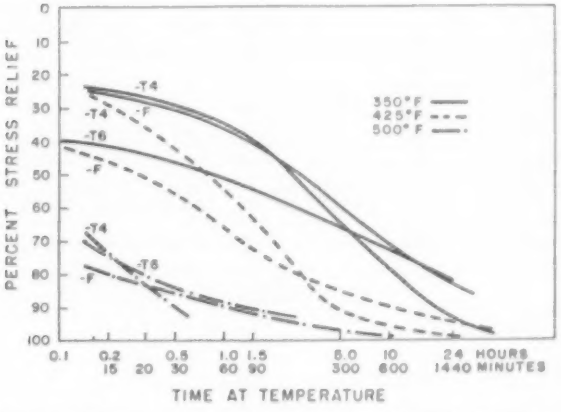

Fig. 11 Relaxation of AZ63A in the -F, -T4 and -T6 conditions

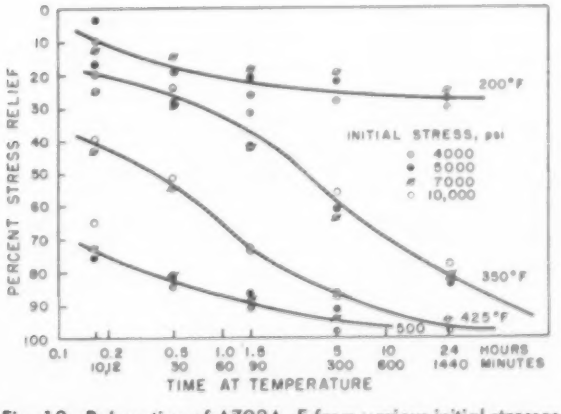

Fig. 12 Relaxation of AZ92A -F from various initial stresses at different temperatures

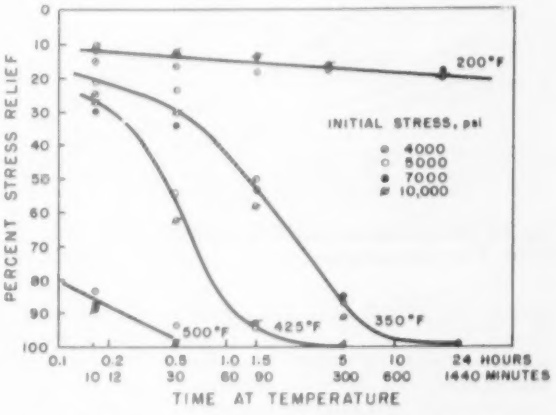

Fig. 13 Relaxation of AZ92A -T4 from various initial stresses at different temperatures

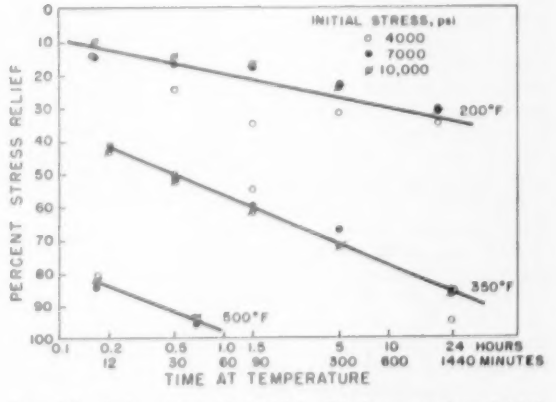

Fig. 14 Relaxation of AZ92A -T6 from various initial stresses at different temperatures

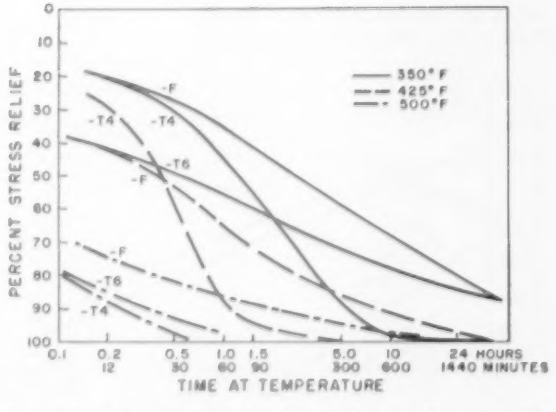

Fig. 15 Relaxation of AZ92A in the -F, -T4, and -T6 conditions

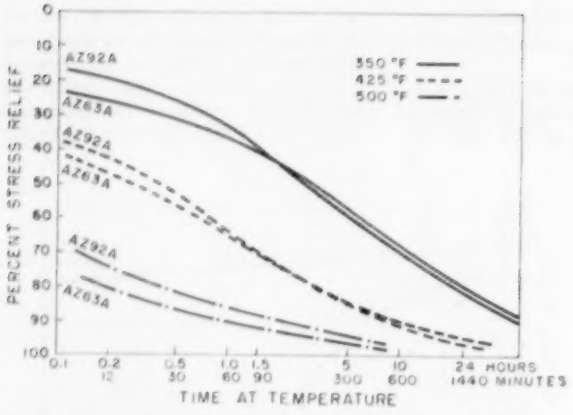

Fig. 16 Relaxation of AZ63A and AZ92A in the -F or cast condition

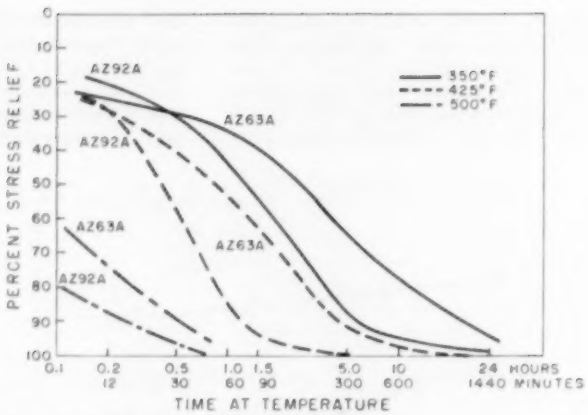

Fig. 17 Relaxation of AZ63A and AZ92A in the -T4 condition

than for the AZ63A, as shown in Figs. 17 and 18.

Often, some given temperature other than investigated might be required for stress relief and therefore temperature-time curves for a given percent of stress relief have proved quite useful. To meet those possibilities, curves for 95, 90 and 80% relaxation for the two casting alloys in the -T4 and -F conditions have been plotted and are shown in Figs. 19–22. The curves for 95% stress relief for both alloys in the two tempers have been combined in one graph in Fig. 23. This graph could be particularly useful where 95% stress relief, such as for dimensional stability after machining, were required.

The ZK51A alloy was studied only in the -F or as-cast temper. Stress levels of 5000 and 10,000 psi were used with heating temperatures of 350, 400, 500 and 600° F. The specimens were 0.2125 in. thick, with the edges machine-contoured to the cantilever shape while the stressed surfaces were as-cast. The relaxation data with the resulting curves appear in Fig. 24. At 350 and 400° F, only 60% stress relief was obtained after 24 hr. At 500° F, 95% stress relief occurred after 24 hr, while at 600° F, only 2 hr were required to achieve 95% stress relief. The curves indicated that the degree of stress relief at 350 and 400° F was independent of the initial stress, while at 500 and 600° F, a higher rate was obtained with a higher initial stress. It could be concluded from the results that stress relief of ZK51A would be most practical at 600° F because the heating time would be relatively short.

Discussion

Specimen Types

Specimen preparation in the wrought materials offered no unusual problems. In the cast materials, three choices were available, those with as-cast surfaces, completely machined or as-cast surfaces with machined edges. The reproducibility of each condition was as below:

Specimen condition	*Test Time, min*	*Test Temp, °F*	*Stress relief, % Test 1*	*Stress relief, % Test 2*
As-cast surfaces	90	350	42	42
Completely machined as-cast surfaces	90	350	53	49.1
Contour machined edges	90	350	44.5	45.9

The higher rates of relief in the machined specimens were due to disturbances in surface stresses caused by the machining operation. As a result, the wholly cast or machined-edge specimens were used in the tests reported herein. An estimation of experimental errors, including measurements of thickness, width, location of loading point and load weights indicated a possible accumulated maximum error of 4%, with no significant differences in the two methods of specimen preparation.

Wrought Alloys

The relaxation curves of the wrought alloys indicated straight line relationships, but, if plotted on rectangular coordinates, the curves would show the usual rapid reduction in residual stress

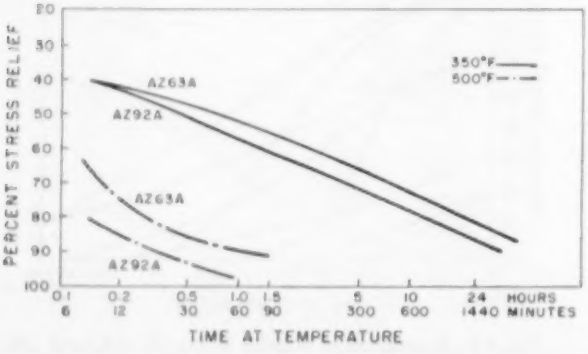

Fig. 18 Relaxation of AZ63A and AZ92A in the -T6 condition

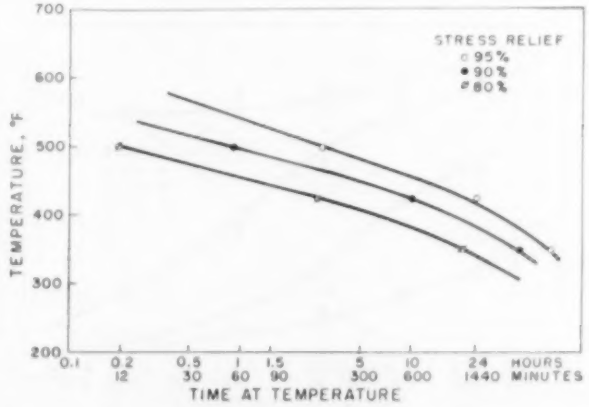

Fig. 19 Time-temperature conditions for AZ63A-F at 95, 90 and 80% stress relief

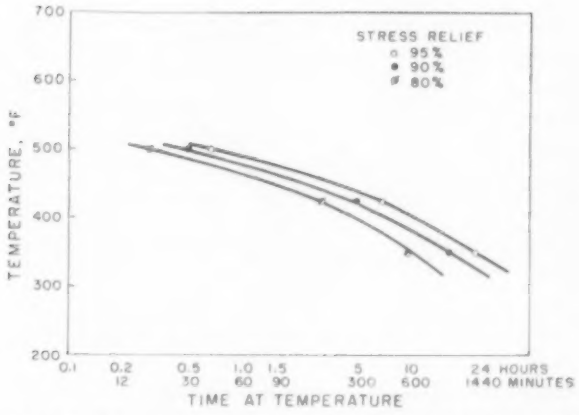

Fig. 20 Time-temperature conditions for AZ63A -T4 at 95, 90 and 80% stress relief

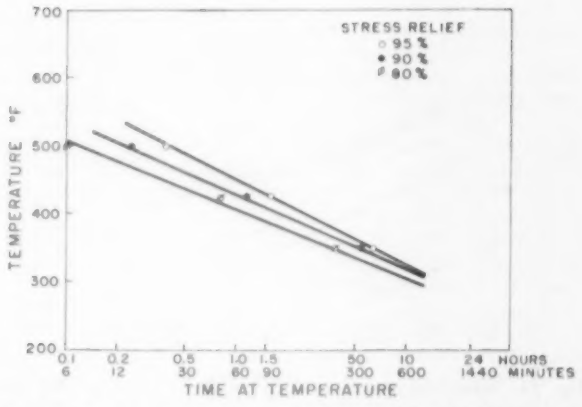

Fig. 22 Time-temperature conditions for AZ92A -T4 at 95, 90 and 80% stress relief

initially with appreciably slower rates on extended heating time. The relaxation rates were affected to a greater degree by temperature changes than by holding time, and therefore, when necessary to change relaxation conditions, a change in temperature rather than in holding time, would be indicated.

The curves for M1A and AZ31A indicated that their relaxation rates were independent of the initial stresses, up to at least 15,000 psi. However, the relaxation curves for ZK60A in both tempers showed that the relaxation rates were higher for higher initial stresses.

The curves for the wrought alloys also indicated that the rate of relaxation was dependent on the structural condition of the materials. The work-hardening alloys M1A and AZ31A in the -H24 or work-hardened temper exhibited faster relaxation rates than the -0 or annealed temper at a given temperature and required lower temperatures for a given rate of relaxation. The occurrence of rapid flow or relaxation in work-hardened materials at relatively low temperatures without detectable recrystallization but with a slight change in some of the mechanical properties is a metallurgical phenomenon designated as recovery. This is, in a sense, an annealing or softening operation but without detectable metallurgical change. The precipitation hardening ZK60A alloy showed more rapid relaxation in the as-extruded than in the precipitation hardened (-T5) condition. This difference in relaxation rates could be attributed to the higher yield and creep strength resulting from the precipitation or to a metallurgical instability in the extruded state like the recovery in work-hardened material.

A comparison of the three alloys showed that their relaxation rates increased in the order M1A, AZ31A and ZK60A. This also was the order of increasing creep rates.

Cast Alloys

The commercial casting alloys of the Mg-Al-Zn type are of the heat treatable variety. Therefore, reheating for relief of residual stresses could result in changes of mechanical properties as well as relaxation rates, depending on the condition and temperatures used. A comprehensive survey of property changes and dimensional stability in AZ92A and AZ63A when heated to various aging temperatures was made by Leontis and Nelson.[3] These authors covered heating temperatures up to 500° F, which also were the stress-relieving temperatures covered by this work. They showed that the rates of aging and growth were greater for the higher alloy content AZ92A than for the AZ63A. Higher strengths and lower ductilities were also exhibited by AZ92A. The aging process was accompanied by growth, which could be used as a measure of the degree of aging or precipitation. A comparison of the relaxation curves for the two alloys with the data by Leontis and Nelson showed that relaxation curves followed closely the trends in aging. These trends are illustrated in Fig. 25 in which the relaxation, growth and hardness changes of AZ92A-T4 were plotted for 350 and 425° F. The abrupt increase in relaxation coincided with the greatly increased

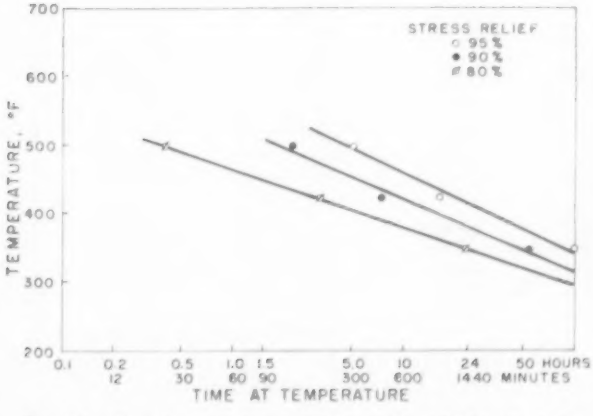

Fig. 21 Time-temperature conditions for AZ92A -F at 95, 90 and 80% stress relief

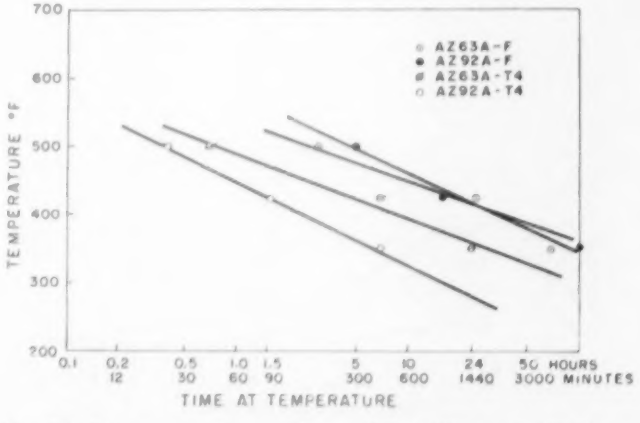

Fig. 23 Time-temperature conditions for AZ63A and AZ92A at 95% stress relief

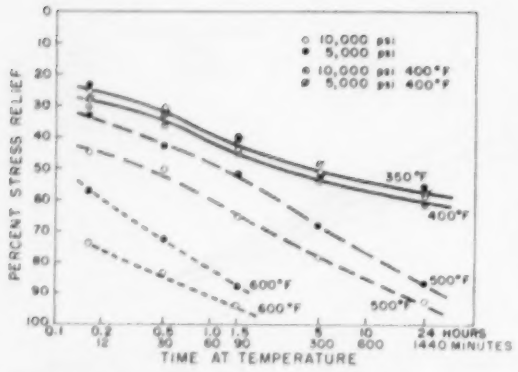

Fig. 24 Stress relaxation of ZK51A -F at different temperatures

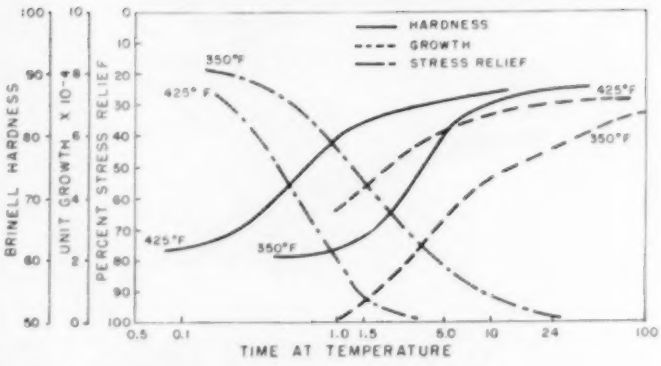

Fig. 25 Stress relief, hardness and growth curves for AZ92A -T4 at 350 and 425° F

aging and growth, indicating that the rate of relaxation was greatly influenced by the growth resulting from the precipitation effects. Similar correlation was also observed with AZ63A-T4. The relaxation curves of the two alloys at 350 and 425° F for the -F temper showed trends similar to those observed in the -T4 temper but to a lesser degree. This also was in line with the aging results of Leontis and Nelson, who showed that some precipitation and growth occurred in the as-cast materials as a result of the retention of a substantial amount of aluminum and zinc in solid solution on solidification and cooling down of these alloys. While the effects of low temperatures on relaxation were interesting and the reasons for the unusual shapes of the curves could be explained by Leontis and Nelson on the basis of the aging, the results were of less importance practically than those obtained at 500° F. Reference to the series of curves for all tempers in Figs. 11 and 15 showed that for practical purposes, relaxation treatments at 500° F for 1 hr resulted in better than 95% stress relief for AZ63A-T4, AZ92A-T4 and AZ92A-F, and better than 85% for AZ63A-F, AZ63A-T6 and AZ92A-T6. Therefore, the stress-relief recommendations for welded Mg-Al-Zn castings currently used in the magnesium industry comprises the heating of weldments to 500° F and holding for 1 hr.

The ZK51A cast alloy compared very closely in composition with the wrought ZK60A, the difference being only in the zinc content. The relaxation curves of the two alloys, appearing in Figs. 6, 7 and 24, showed that the cast metal exhibited much lower rates of relaxation and required higher temperatures or longer times. At 350° F, only 60% relaxation was obtained after heating for 24 hr as compared to 90% relaxation of the ZK60A-T5 after 1 hr. The ZK51A alloy showed a rate of relaxation at the lower temperatures which was independent of the magnitude of the initial stress but at the higher temperatures the rate of relaxation was greater with higher stresses. For 95% stress relief, heating to 600° F for 2 hr would be required.

British Relaxation Data

R. J. M. Payne reported[2] the results of stress-relief studies on the British AZ91, A8, Z5Z and MCZ cast alloys. The technique used was to split a cast ring, $3^1/_2$-in. OD, $1^1/_2$-in. thick and 1-in. deep. A $^1/_8$-in. slot was made in the ring with gage marks scribed on either side of the slot. A $^3/_{16}$-in. wedge was forced into the slot and the amount of spring-back after heat treatment was measured by a tracking microscope.

Payne's data for A8 and AZ91 have been replotted in Figs. 26 and 27 in terms of percent stress relief. Since the lowest temperature used was 482° F (250° C), the effects of precipitation in the as-cast alloys were missed because there was a marked decrease in growth at temperatures above 425° F and a rapid increase in the solid solubility of aluminum and zinc. A fair agreement between Payne's data on AZ91 and the present data on AZ92A-F is indicated by comparison of Figs. 12 and 27. Payne also showed that stress relaxation of AZ91 and A8 was independent of the magnitude of the initial stress, thus agreeing with the data on AZ92A and AZ63A in this paper. For 95% stress relief of AZ91 and A8, his data showed heating for 2 hr at 626° F (330° C) was required. It was also shown that the properties of the two alloys were not significantly affected by this treatment.

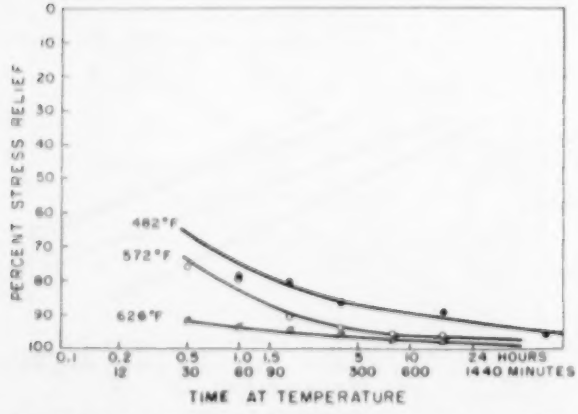

Fig. 26 Relaxation of British A8 alloy in as cast condition. Data from R. J. M. Payne[2]

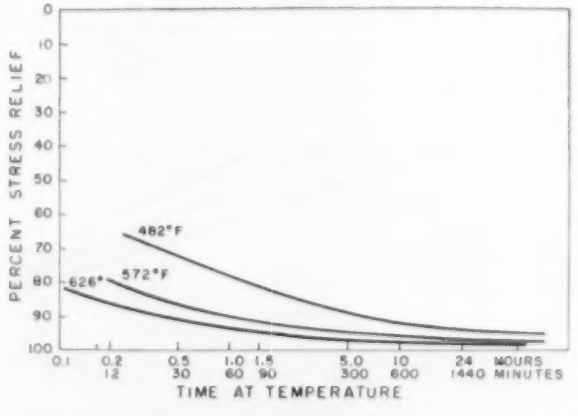

Fig. 27 Relaxation of British AZ91 alloy in as cast condition. Data from R. J. M. Payne[2]

Table 3—Stress Relief Treatments for Some Commercial Magnesium Alloys after Welding

Sheet			Extrusions			Castings*		
Alloy	Temp, ° F.	Time, min	Alloy	Temp, ° F	Time, min	Alloy	Temp, ° F	Time, min
AZ31A-0	500	15	AZ31A-F	500	15	AZ63A	500	60
AZ31-H24	300	60	AZ61A-F	500	15	AZ92A	500	60
M1A-O	500	15	AZ80A-F	500	15	AZ91C	500	60
M1A-H24	400	60	AZ80A-T5	400	60			
			ZK60A-F	500	15			
			ZK60A-T5	300	60			

* Treatment applies to -F, -T4, -T5 and -T6 tempers.

Practical Considerations

The chief reason for stress-relieving magnesium alloys is to prevent stress-corrosion failures of parts after welding. The internal stresses after welding may be as high as the proportional limit and have been observed to reach 15,000 psi in some wrought alloys. Stress relief is also useful in maintaining dimensional tolerances after machining when a high degree of precision is required.

The phenomenon of stress-corrosion cracking of welds has been observed in most other metals as well as magnesium under proper conditions. In magnesium alloys, only the Mg-Al-Zn series are susceptible to stress-corrosion cracking. Therefore, of the wrought alloys studied, only the AZ31A material is in this class.[6] The M1A and ZK60A are not stress-corrosion sensitive and therefore do not require stress-relief treatments after welding. These two alloys are not used extensively in welded applications because of the low weld efficiency in the case of the M1A and because of limited weldability of the ZK60A.

In the field of casting alloys, the Mg-Al-Zn variety, consisting of the American AZ63A, AZ92A and AZ91C as well as the British A8 and AZ91 are also subject to stress corrosion after welding and therefore require stress-relief treatments. Internal stresses may be present in some cast materials either as a result of casting or machining effects. These are generally of a low order of magnitude and cause no difficulties from stress corrosion but may have a considerable effect on tolerances after machining, where a high degree of precision is required. Therefore, for dimensional precision after machining, stress relief may be required on all cast alloys.

A minimum of 95% stress relief is a commonly accepted requirement where dimensional stability or tolerances have to be controlled. Where the primary concern is stress corrosion, a minimum of 80% stress relief has proved adequate in all commercial magnesium alloys.

Another class of materials of importance in welded applications is the extruded alloys. Here again the Mg-Al-Zn alloys require stress-relief treatments after welding to prevent stress-corrosion failures. In most respects, the extruded, nonheat-treatable alloys behave similarly to the annealed sheet and can be heated to relatively high temperatures, up to at least 600° F, without significantly affecting the mechanical properties. The precipitation-hardening alloys require a specific knowledge of maximum heating temperatures to prevent loss of properties due to overaging.

The currently used stress-relief treatments for welded commercial magnesium alloys are shown in Table 3. These treatments have proved satisfactory in eliminating stress-corrosion failures of welds under service conditions for the past several years. While no stress-relaxation data are available for AZ31A, AZ61A and AZ80A extrusion alloys to predict stress-relief conditions, nevertheless, the treatments given in the table have been based on stress-corrosion tests conducted on welds annealed under various conditions. It should be emphasized that the treatments listed in Table 3 are primarily for stress relief of weldments and may not be applicable where dimensional stability is required, since only 80% stress relief is set up as the minimum value, whereas a minimum of 95% is indicated for stability requirements. When 95% stress relief is required, reference should be made to the original curves for selection of suitable conditions. In some cases, test data were not carried out to sufficiently low stress levels to permit the selection of actual conditions but in most cases the data will permit an intelligent estimate of the most probable conditions.

In the preceding discussions, no mention of local stress relief as a method of reducing internal stresses was considered. However, the feasibility of local stress relief for prevention of stress corrosion of welds has been frequently raised. The subject has not been sufficiently investigated to permit definite conclusions but on the basis of some exposure tests and recent measurements of residual stresses in welds, indications were that local heating provided satisfactory stress relief. The procedure currently recommended for both wrought and cast Mg-Al-Zn alloys is to heat the area in question with a gas torch to 600° F and maintain the temperature for 10 min. The part is allowed to cool slowly and away from drafts. Local heating has the drawbacks of uncertainty in the heating conditions and the possibility of inducing additional stresses, particularly in highly restrained or complicated parts. The best assurance for the removal of internal stresses is to heat the entire part whenever possible.

Summary

1. Stress-relaxation measurements by the use of the simple and cantilever beam are described.

2. The stress-relaxation curves for the wrought alloys AZ31A, M1A and ZK60A and/or the cast alloys AZ63A, AZ92A and ZK51A are presented. The curves for British alloys A8 and AZ91 based on R. J. M. Payne's data are also included for comparison.

3. The practical applications and significance of relaxation data to stress-relief treatments of the commercial alloys are pointed out.

4. The stress-relief treatments for a number of commonly used commercial magnesium alloys are summarized in Table 3.

5. The information presented in this paper is intended to be factual and of use to engineers and fabricators as a guide in meeting problems arising from fabrication or machining processes.

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PROPERTIES AND CHARACTERISTICS OF A QUENCHED AND TEMPERED STEEL FOR PRESSURE VESSELS

Results of test program designed to determine tensile properties, notch toughness, metallurgical characteristics, welding characteristics, gas-cutting characteristics and fatigue properties of the steel

BY W. D'ORVILLE DOTY

Introduction

This paper concerns a new steel for pressure vessel construction. This steel—"T-1"*—was developed to meet the need for a constructional steel, primarily in plate form, possessing in the quenched and tempered condition a minimum yield strength of 90,000 psi, good low-temperature toughness and good weldability. In addition to these characteristics, the steel in the quenched and tempered condition was designed to possess sufficient ductility to undergo bending to reasonable radii, to have satisfactory machinability and to be amenable to gas cutting without special care. As will be evident from the test results described in this paper and in a companion paper[1] these goals have been achieved.

A previous paper[2] described the laboratory research work which lead to the development of "T-1" steel, provided data on plates from the first commercial heat of the steel and described many of the engineering potentialities of the new steel. The previous paper also stated that arrangements were being made to build and test several full-scale pressure vessels made of "T-1" steel in order to demonstrate its suitability for pressure vessel construction. The vessel tests have now been completed. In addition, laboratory tests have been conducted on plate samples from the same heat used in the test vessels in order to determine the tensile properties, the notch toughness, the metallurgical characteristics, the welding characteristics, the gas-cutting characteristics and the fatigue properties of the steel.

This paper describes the results of many laboratory tests on plate samples from the pressure-vessel-test heat and certain other heats. The tests on the full-scale pressure vessels are described in the forementioned companion paper.[1] A third paper discusses design of pressure vessels made of "T-1" steel.[3]

Composition and Mechanical Properties

The chemical composition range, by ladle analysis, for "T-1" steel is given in Table 1. The table also shows the chemical composition of the 1/2-in-thick plates from Heat No. 74L236—the pressure-vessel-test heat. The steel is a low-carbon alloy steel with properties attained by quenching and tempering. It may be noted in the table that the carbon content is low, not greater than 0.20%. A low-carbon level is essential for good weldability; furthermore, toughness is most pronounced at low-carbon levels. The steel contains the alloying elements manganese, nickel, chromium, molybdenum, vanadium, copper and boron in carefully selected quantities in order to permit the attainment of high-yield strength in combina-

Table 1—Chemical Composition of "T-1" Steel

	Composition Range, % (ladle analysis)	*Heat No. 74L236 ladle analysis, %*
Carbon	0.10/0.20	0.15
Manganese	0.60/1.00	0.92
Phosphorus	0.040 max.*	0.014
Sulfur	0.050 max.*	0.020
Silicon	0.15/0.35	0.26
Nickel	0.70/1.00	0.88
Chromium	0.40/0.80	0.50
Molybdenum	0.40/0.60	0.46
Vanadium	0.03/0.10	0.06
Copper	0.15/0.50	0.32
Boron	0.002/0.006	0.0031

* Phosphorus 0.035% max. and sulfur 0.040% max. for firebox quality.

Table 2—Mechanical Properties of "T-1" Steel

	Thickness 1/4 to 2 in., incl.		
Yield strength (min.), ext. under load, psi*	90,000		
Tensile strength (min.), psi	105,000		
Elongation in 2 in., % (min.)	18		
Reduction of area, % (min.)	55†		
	1/2 to 1 in., incl.	*Over 1 to 2 in., incl.*	
Cold bend‡	180°, $D = 2t$	180°, $D = 3t$	

* Yield strength values shown elsewhere in this paper are based on 0.2% offset.

† A standard 0.505-in. diam tension specimen is used if the thickness exceeds 3/4 in. On sizes 3/4 in. and under, a strap type tension specimen may be used; however, the reduction of area specification is then lowered to 45% minimum.

‡ Cold bend tests are not ordinarily specified. If specifically requested, these values can be met.

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* "T-1" is a trademark of the United States Steel Corp.

tion with high toughness and good weldability. The element boron allows the use of relatively small percentages of other alloying elements such as nickel and molybdenum which are less available and more expensive.

"T-1" steel in the plate thickness range $^1/_4$ to 2 in., inclusive, is furnished to the mechanical properties given in Table 2. These properties are imparted to the plates by the following heat treatment:

(1) Water quenching cold from an austenitizing temperature in the range 1650 to 1700° F, and
(2) tempering in the range of 1150 to 1200° F, followed by water quenching cold.

The effect of this heat treatment on the tensile and impact properties of plate within the above-mentioned thickness range is illustrated in Fig. 1. The

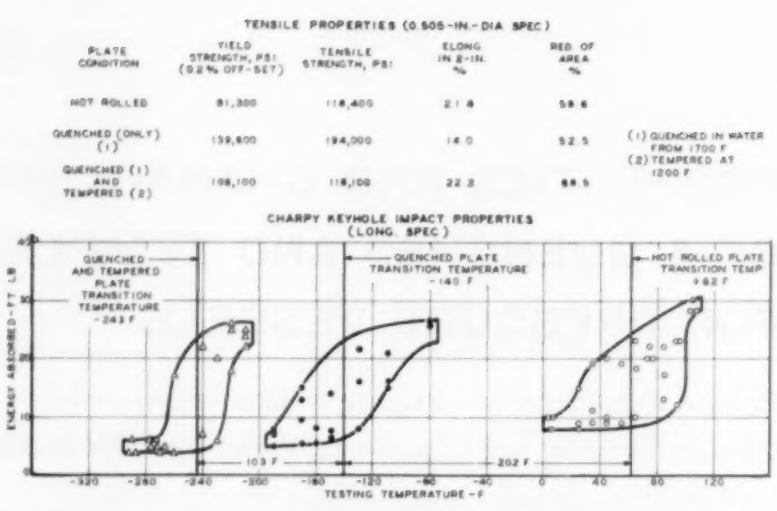

Fig. 1 Effect of heat treatment on mechanical properties of "T-1" steel

Table 3—Mechanical Properties of "T-1" Steel Plates Used in Test Vessels

(All plates $^1/_2$ in. thick, firebox quality; Heat No. 74L236)

Slab No.	*Orientation and location of specimen*	*Yield Strength (0.2% off-set), psi*	*Tensile strength, psi*	*Elastic ratio, %*	*Elong. in 8 in., %*	*Elong. in 2 in., %*	*Charpy keyhole Impact Value at −50° F, ft-lb*	*Charpy keyhole Ductility-transition temperature, ° F**	*Hardness (DPH)*
62670	Long. bot.	119,700	123,300	97.0	...	27	34-33-32	−217	272
	Long. top	124,400	127,800	97.4	...	28			
	Trans. top	124,400	129,200	96.3	...	27	23-22-22		
62672	Long. bot.	107,300	113,150	94.9	...	32.0	33-31-33	−197	260
	Long. top	106,700	115,650	92.2	...	29.0			
	Trans. top	111,000	117,400	94.4	...	31.0	29-28-27		
62674	Long. bot.	115,600	121,600	95.1	10.7	29.0	32-32-32	−217	272
	Long. top	111,000	117,100	94.5	11.5	31.0			
	Trans. top	112,200	119,200	94.1	10.7	27.0	24-25-24		
62675	Long. bot.	121,400	128,300	94.5	11.0	31.0	31-31-28	−208	294
	Long. top	123,100	128,800	95.8	11.7	26.0			
	Trans. top	123,800	130,700	94.6	8.5	26.0	22-23-21		
62676	Long. bot.	116,300	123,300	94.3	10.5	28.0	33-35-34	−225	284
	Long. top	110,500	117,500	94.1	10.2	28.0			
	Trans. top	112,300	121,400	92.5	11.0	23.0	26-25-26		
62677	Long. bot.	121,400	127,000	95.6	10.2	28.0	32-32-30	−200	283
	Long. top	114,600	121,400	94.4	11.0	31.0			
	Trans. top	117,100	123,700	94.9	8.0	22.0	25-25-25		
62678	Long. bot.	114,500	122,400	93.5	13.0	27.0	30-30-30	−212	277
	Long. top	111,600	120,900	92.3	10.7	25.0			
	Trans. top	113,900	123,600	92.1	11.5	29.0	27-28-27		
62679	Long. bot.	124,800	133,600	93.4	10.5	23.0	29-31-33	−235	298
	Long. top	115,100	131,200	87.7	9.7	28.0			
	Trans. top	125,500	135,300	92.7	10.5	28.0	25-24-25		
62680	Long. bot.	114,100	123,100	92.7	10.7	29.0	36-34-34	−205	275
	Long. top	107,700	115,100	93.3	15.2	32.0			
	Trans. top	109,600	117,200	93.4	10.5	27.0	27-28-28		
62681	Long. bot.	116,100	128,600	90.5	12.0	27.0	31-30-31	−238	286
	Long. top	119,900	126,700	94.5	11.7	22.0			
	Trans. top	120,600	129,200	93.2	10.7	26.0	23-23-22		
62682	Long. bot.	125,300	132,900	94.4	11.5	28.0	29-27-30	−208	296
	Long. top	120,900	127,900	94.5	13.2	31.0			
	Trans. top	119,100	131,000	91.0	10.0	27.0	22-22-22		
62683	Long. bot.	126,700	132,200	95.5	10.2	27.0	34-30-31	−235	292
	Long. top	121,050	128,200	94.4	13.0	25.5			
	Trans. top	125,100	132,000	94.8	9.0	25.0	22-22-21		
62687	Long. bot.	125,500	133,300	94.1	9.0	27.0	31-30-30	−230	307
	Long. top	120,700	127,300	94.6	10.5	28.0			
	Trans. top	116,800	131,100	88.9	9.2	24.0	22-20-21		
62688	Long. bot.	108,500	117,000	92.7	...	29.0	29-32-29	−194	255
	Long. top	112,700	121,600	92.6	...	28.0			
	Trans. top	118,000	124,550	94.8	...	27.0	24-23-22		

* Longitudinal specimen.

ductility-transition temperature selected at the middle of the "scatter band" in the Charpy-keyhole-notch curve was used as the criterion for evaluating the notch toughness of the steel. It may be noted that the hot-rolled plate has a yield strength less than 90,000 psi and a ductility-transition temperature near 60° F. Quenching the plate to form a martensitic microstructure raised the yield strength and greatly improved the low-temperature notch toughness. The minus 140° F transition temperature for the as-quenched plate may seem surprisingly good in comparison with the known poor toughness of martensite in high-carbon steels. However, such good low-temperature toughness is characteristic of low-carbon martensite. Tempering resulted in a further improvement in notch toughness, a moderate lowering of the yield strength and a doubling of the tensile elongation. It should be borne in mind that the data on notch toughness are based on 1/4 to 2-in.-thick material. However, it is well known that toughness decreases as the plate thickness increases.

The mechanical properties of the many heat-treated plates used in the construction of the test vessels previously mentioned are given in Table 3. These data were obtained on "cross-rolled" plate and illustrate the similarity between longitudinal tensile and transverse tensile properties and between the tensile properties from specimens taken from the top and from the bottom of each unit plate rolled from a slab. It may also be noted for the plates rolled from the 14 slabs that the yield strengths range from 106,700 to 126,700 psi, well above the 90,000 psi minimum for the steel.

As may be seen in Table 3 the ductility-transition temperatures by the Charpy-keyhole-impact test range from −194 to −238° F. The value for plate from Slab No. 62678 is −212° F. This transition temperature for longitudinally oriented Charpy-keyhole specimens may be compared in Fig. 2 with the transition temperatures obtained from transversely oriented specimens, similarly notched, and from longitudinally oriented and transversely oriented Charpy V-notch specimens. The ductility-transition temperature by the Charpy V-notch test was selected at the 10 ft-lb level. It can be seen that regardless of specimen orientation the transition-temperature values for similarly notched specimens are nearly the same. The transition by the Charpy V-notch test is about 25° F higher than that by the Charpy-keyhole test.

The effect of plate thickness in the range 3/4 to 2 in., inclusive, on the tensile and impact properties of "T-1" steel is shown in Table 4. It may be noted that the tensile properties and the low-temperature notch toughness vary only a relatively small amount. The effect of plate tempering temperature on ductility-transition temperature is given in Table 5. The data show lower transition temperatures for higher tempering temperatures. The effects of tempering temperature on hardness and strength will be discussed later in this paper.

Table 4—Effect of Plate Thickness on Mechanical Properties of "T-1" Steel

	*Tensile properties**			
Plate thickness, in.	*Yield strength (0.2% offset), psi*	*Tensile strength, psi*	*Elong. in 2 in., %*	*Red. of area, %*
3/4	111,300	122,250	21.0	65.8
1	122,800	128,800	21.0	68.1
1 1/2	124,800	131,300	20.0	65.9
1 3/4	123,550	130,100	21.0	70.2
2	119,150	128,200	20.5	68.0

	Charpy-keyhole impact properties†	
Plate thickness, in.	*Orientation of Specimen in plate*	*Ductility-transition temp., ° F*
1	Longitudinal near midthickness	−195
1 3/4	Longitudinal near midthickness	−212

* Heat 348267.
† Heat 298144.

Table 5—Effect of Tempering Temperature on Ductility-Transition Temperature of "T-1" Steel

Charpy-keyhole impact test. Longitudinal specimens from 1-in.-thick plate*

Tempering temperature, ° F.	*Ductility-transition temperature, ° F.*
1100	−150
1200	−170
1250	−180
1300	−205

* Heat 348267.

The transition temperatures of unstrained, strained, strained and aged, and strained and stress-relieved specimens of plates from each of two heats of "T-1" steel are shown in Table 6. It is apparent from the data that for the specimen conditions investigated, the two "T-1" steel plates have about the same low susceptibility to straining and nearly the same low susceptibility to strain aging. Although there is no absolute correlation between transition temperatures as obtained experimentally with the Charpy-keyhole-impact test and the service performance of pressure vessel steels, the data in Table 6 suggest that the amounts by which the low transition temperature of "T-1" steel is raised by straining or strain aging are probably unimportant for most commercial applications involving cold-formed plates.

Heat-Treating Characteristics

Typical photomicrographs of the structure in hot-rolled, as-quenched, and quenched and tempered 1/2-in.-thick plates are given in Fig. 3. The structure in the hot-rolled plate is predomi-

Table 6—Effect of Straining and of Straining and Aging on Ductility-Transition Temperature of "T-1" Steel

Charpy-keyhole impact test.
Longitudinal specimens from 3/4-in.-thick plate

	Heat 368462		*Heat 478464*	
	Ductility-transition temp., ° F	*Difference in transition temp., ° F*	*Ductility-transition temp., ° F*	*Difference in transition temp., ° F*
Unstrained*	−255	...	−175	...
Strained 10% (cold reduced)	−222	+33	−155	+20
Strained 10% and artificially aged for 1 hr at 550° F	−192	+63	−135	+40
Strained 10% and stress relieved at 1100° F for 1 hr	−273	−18	−180	−5

* In the quenched and tempered condition.

nantly proeutectoid ferrite and high-carbon martensite with possibly some bainite. The high-carbon martensite is a consequence of the ferrite precipitation. The structures in the as-quenched plate and in the quenched and tempered plate are low-carbon martensite and tempered low-carbon martensite, respectively. An end-quench hardenability curve for steel from the pressure-vessel-test heat is shown in Fig. 4. For a distance of about 0.38 in. along the bar from the quenched end the microstructure is at least 95% martensite. The structure is 50% martensite and the balance bainite at 0.5 in. and 100% bainite at 1 in.

The relationship between hardness, tensile strength and tempering temperature for "T-1" steel is presented in Fig. 5. It may be noted that a tensile strength of at least 105,000 psi is attained even at relatively high tempering temperatures. The retardation of the loss of strength with increasing tempering temperatures in the range 900 to 1100° F results from the precipitation of fine vanadium carbide. A similar relationship exists for yield strength; therefore, if a plate after heat treatment has a yield strength lower than 90,000 psi the plate need only be reheated to 1675° F, water quenched and then tempered at a temperature somewhat lower than that previously used in order to obtain a higher yield strength. This method of attaining desired tensile properties is quite different from that for hot-rolled steel in which the resulting properties are principally a function of composition, finishing temperature and cooling rate.

A temperature-dilation curve for "T-1" steel is shown in Fig. 6. The average linear coefficient of expansion over the temperature range of 68 to 1360° F is calculated to be 7.74×10^{-6} per degree Fahrenheit (13.93×10^{-6} per degree Centigrade). This value is within the range of comparable values for carbon and medium alloy steels.

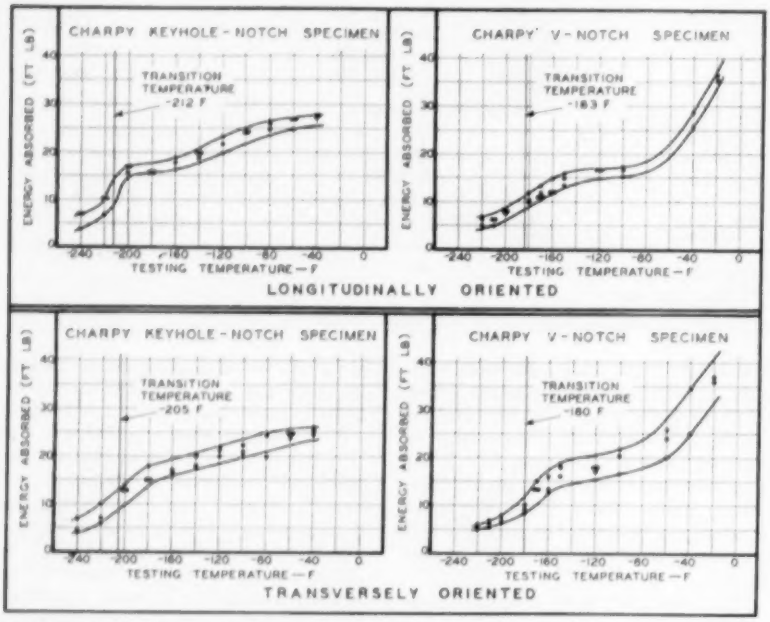

Fig. 2 Effect of type of notch on transition temperature by the Charpy test—$^1/_2$-in. "T-1" steel

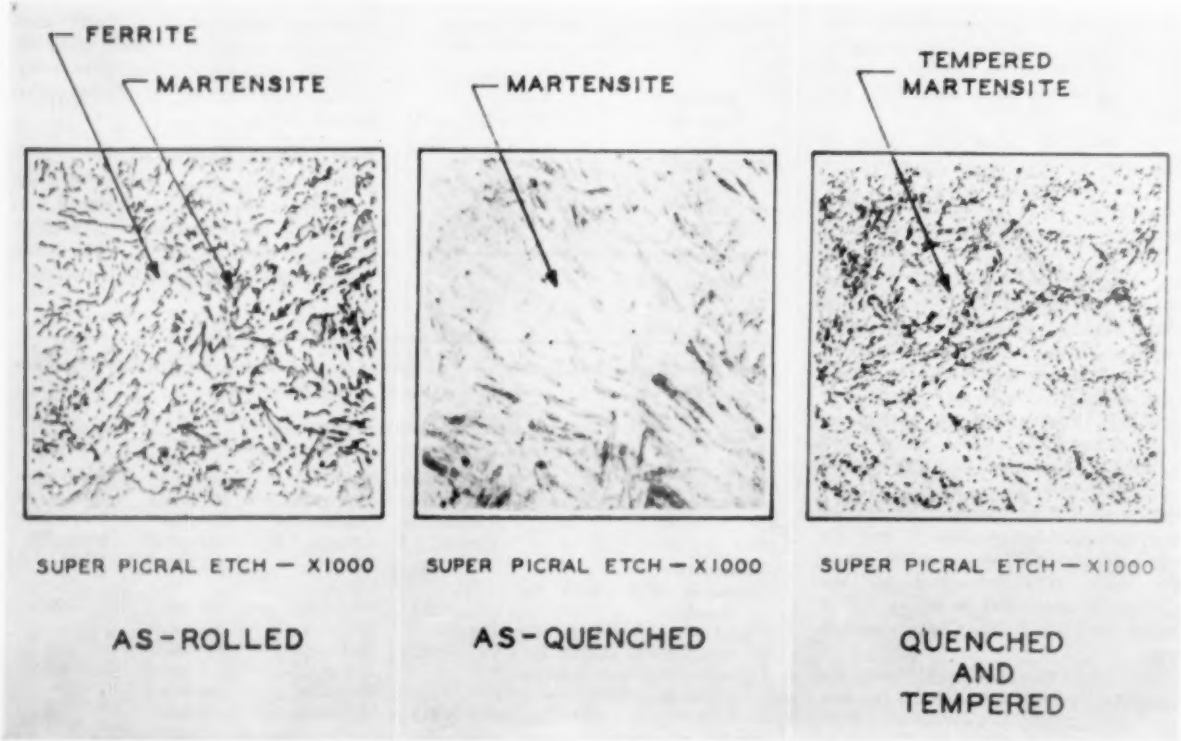

Fig. 3 Typical photomicrographs of $^1/_2$-in. "T-1" steel before and after final heat treatment

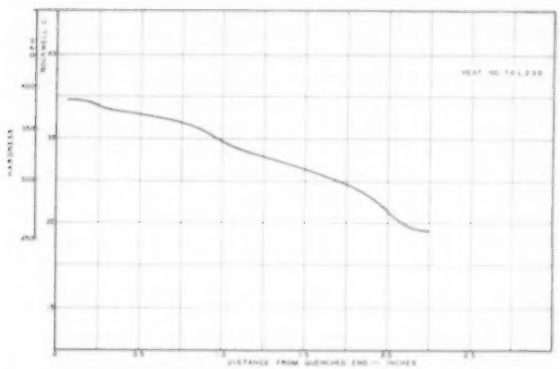

Fig. 4 End-quench hardenability curve for "T-1" steel

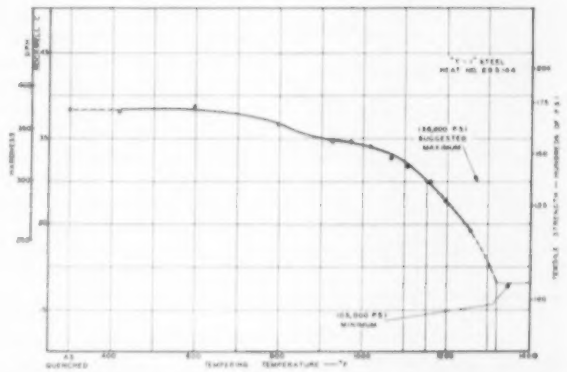

Fig. 5 Relationship between hardness, strength and tempering temperature

Figure 7 illustrates the isothermal-transformation diagram of "T-1" steel and serves to show the influence of the particular combination of alloying elements in retarding at temperatures above about 950° F the transformation to ferrite and pearlite. A "bainite knee" juts out to the left below 950° F.

Properties of Welded Joints

Previous studies[2] have shown that low-hydrogen electrodes should be used for the welding of "T-1" steel unless a high-temperature preheat is employed, and that high-strength electrodes of the 120,000 psi tensile-strength class are necessary in order to develop the full strength of the welded steel. It was also shown that the ductility-transition temperature by the Charpy-keyhole test of as-deposited E12015 weld metal is sufficiently low that such weld metal would be suitable for "T-1" steel structures not stress relieved. However, the transition temperature of stress-relieved E12015 weld metal is very high; thus making the weld metal unsuitable for those structures which are to be given a stress-relief treatment.

For the full-scale pressure vessel test previously mentioned it was desired to compare the performance of as-welded vessels with that of stress-relieved vessels. Since, as described above, suitable stress-relieved vessels could not be made with E12015 electrodes, laboratory tests were conducted on weld metal deposited by other types of electrodes in order to find an electrode suitable for welding the vessels which were to be stress relieved. Tables 7 and 8 give the chemical composition and mechanical properties of several weld metals deposited by high-strength electrodes. The tensile properties were determined from 0.505-in. diam specimens taken from an all-weld-metal pad deposited in the bosom of a structural steel angle. The Charpy impact specimens were prepared from a butt joint in "T-1" steel plate as shown in Fig. 8. The temperature of the steel angle before welding and of the steel

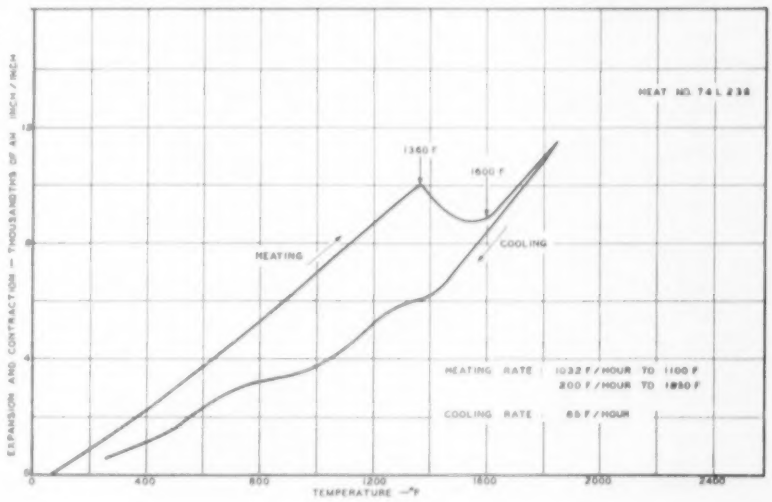
HEAT NO. T-1 L 238
1360 F
1600 F
HEATING
COOLING
HEATING RATE 1032 F / HOUR TO 1100 F
200 F / HOUR TO 1850 F
COOLING RATE 85 F / HOUR
EXPANSION AND CONTRACTION — THOUSANDTHS OF AN INCH / INCH
TEMPERATURE — °F

Fig. 6 Temperature-dilation curve for "T-1" steel

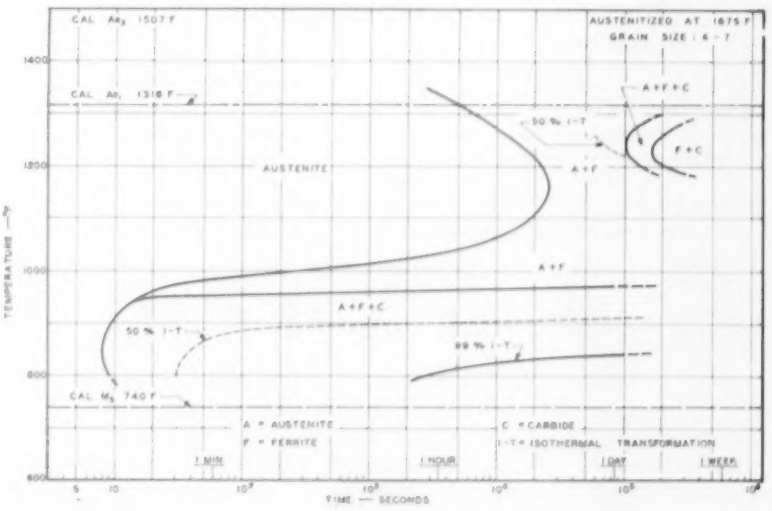

Fig. 7 Isothermal-transformation diagram for "T-1" steel

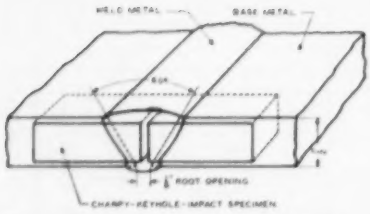

Fig. 8 Location of Charpy-keyhole-impact specimen in weld metal of a butt-joint

plate before welding was 70° F. It will be noted in Table 7 that, either as-deposited or as-stress-relieved, the transition temperature of the E9015 weld metal is superior to that of E12015, as-deposited. However, the good low-temperature notch toughness is gained at a considerable sacrifice in yield strength and tensile strength. Table 8 shows that E10015 (Mn-Mo) weld metal, stronger than E9015 weld metal, has good notch toughness, either as-deposited or as-stress-relieved. However, this weld metal unlike the others described in Tables 7 and 8 is hard, very difficult to chip, and cracks under the high-restraint condition in the Navy patch test (MIL-E-I86A (Ships)). The notch toughness of E10016 (Ni-Mo-V) weld metal, either as-deposited or as-stress-relieved, is insufficient for low-temperature applications. Therefore, it is apparent from the data that for the covered electrodes investigated, only the E9015 (Mn-Mo) type is suitable for making welds which are to be given a stress-relief treatment.

The inert-gas-shielded welds in Table 7 were made by the inert-gas-shielded metal-arc (consumable electrode) process using semiautomatic and automatic equipment. The butt joint from which the impact specimens were taken was made in four passes. All welding was done in an atmosphere of 1% oxygen and 99% argon. It may be noted that, in

Table 7—Chemical Composition and Mechanical Properties of Weld Metals Deposited by High-Strength Electrodes

AWS class.	*Comp. type*	*Weld-metal composition, % (undiluted deposit)* C	Mn	P	S	Si	Ni	Cr	Mo	V	Cu	Ti
E9015	Mn-Mo	0.07	1.45	0.025	0.024	0.39	0.06	0.08	0.35	0.02	0.14	0.013
E12015	Ni-Mo-V	0.06	1.12	0.014	0.018	0.40	2.11	0.11	0.95	0.21	0.11	0.005
Inert-gas shielded	Ni-Mo-V	0.07	1.11	0.017	0.015	0.30	1.19	0.11	0.40	0.09	0.29	0.008

AWS class.	*Comp. type*	*Condition*	*Interpass temp., ° F*	*All-weld-metal tensile properties (0.505-in. diam specimen) Yield strength (0.2% offset), psi*	*Tensile strength, psi*	*Elong. in 2 in., %*	*Red. of area, %*
E9015	Mn-Mo	As-welded	100	88,800*	95,700	16.3	28.1
E9015	Mn-Mo	As-welded	Rising	61,750	84,600	29.0	62.5
E9015	Mn-Mo	Stress relieved (1100° F)	100	87,250*	94,200	23.5	55.7
E9015	Mn-Mo	Stress relieved (1100° F)	Rising	67,500*	82,500	26.8	63.1
E12015	Ni-Mo-V	As-welded	100	113,300	129,200	20.5	58.7
E12015	Ni-Mo-V	Stress relieved (1100° F)	100	116,000	128,700	18.8	59.4
Inert-gas shielded	Ni-Mo-V	As-welded	100	105,800	113,800	21.0	63.9

AWS class.	*Comp. type*	*Charpy-keyhole impact properties Condition*	*Ductility-transition temp., ° F*
E9015	Mn-Mo	As-welded	−155
E9015	Mn Mo	Stress relieved (1100° F)	− 83
E12015	Ni-Mo-V	As-welded	− 78
E12015	Ni-Mo-V	Stress relieved (1100° F)	Above 130
E12015	Ni-Mo-V	Quenched from 1675° F, tempered at 1175° F	45
Inert-gas shielded	Ni-Mo-V	As-welded	−129
Inert-gas shielded	Ni-Mo-V	Stress relieved (1100° F)	− 5

* Yield point.

Table 8—Chemical Compositions and Ductility-Transition Temperatures of Additional High-Strength Weld Metals

AWS class.	*Comp. type*	*Weld-metal composition, % (undiluted deposit)* C	Mn	P	S	Si	Ni	Cr	Mo	V	Cu	Ti
E10015	Mn-Mo	0.16	1.97	0.023	0.017	0.41	0.22	0.16	0.43	0.002	0.14	0.018
E10016	Ni-Mo-V	0.08	0.55	0.018	0.023	0.45	1.97	0.15	0.68	0.09	0.16	0.018

AWS class.	*Comp. type*	*Charpy-keyhole impact properties Condition*	*Ductility-transition temp., ° F*
E10015	Mn-Mo	As-welded	−172
E10015	Mn-Mo	Stress relieved (1100° F)	− 99
E10016	Ni-Mo-V	As-welded	− 28
E10016	Ni-Mo-V	Stress relieved (1100° F)	42

Table 9—Results of Tests of Transversely Butt-Welded Tension Specimens of "T-1" Steel

Electrode type	*Plate thickness, in.*	*Heat No.*	*Condition of weld*	*Reinforcements*	*Tensile strength, psi**	*Location of failure*	*Joint efficiency, %†*
E12015	1/2	36S462	As-welded	Off	118,700	Base metal	100
E12015	1	29S144	As-welded	Off	115,400	Base metal	100
E9015	1	73U115	As-welded	Off	117,100	Weld metal	93.8
E9015	1	73U115	Stress relieved (1100° F)	Off	113,900	Weld metal	91.4
E9015	1	32U029	As-welded	On	121,500	Base metal	100
E9015	1	32U029	Stress relieved (1100° F)	On	117,250	Weld metal	94
E12015	1/2	74L236	As-welded	Off	125,400	Base metal	100
E9015	1/2	74L236	Stress relieved (1125° F)	Off	119,000	Weld metal	95
Inert-gas shielded	1/2	74L236	As-welded	Off	120,100	Heat-affected base metal	100

* Average of three values.
† Based on actual strength of unwelded plate.

general, the weld metal properties are similar to those obtained in comparable joints welded with E12015 electrodes.

Automatic submerged-arc welding has thus far been little used for the welding of "T-1" steel since difficulty has been experienced in obtaining weld metal that has good low-temperature notch toughness in combination with high strength. However, recent preliminary tests on weld metal of a new composition appear promising and further testing is under way.

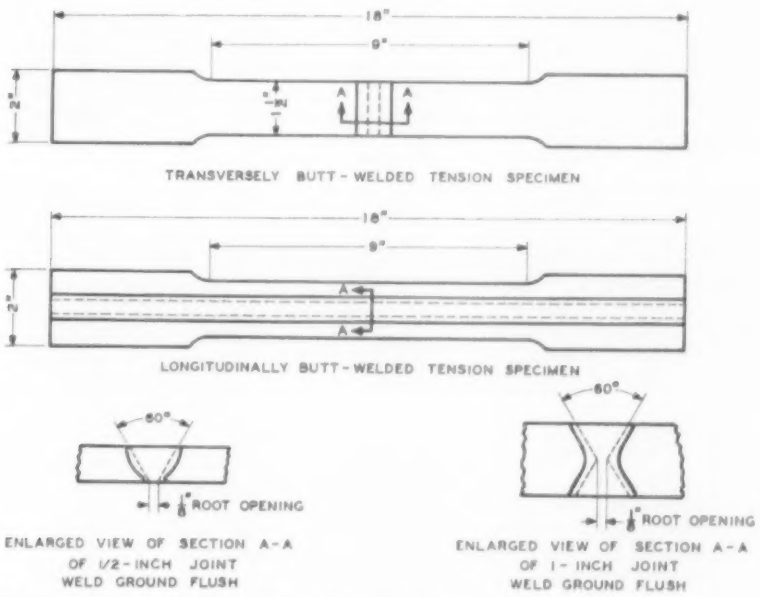

Fig. 9 Details of butt-welded tension specimens

The results of tests of transversely butt-welded tension specimens and longitudinally butt-welded tension specimens, all of quenched and tempered "T-1" steel, are given in Tables 9 and 10 respectively. Details of the specimens are shown in Fig. 9. The temperature of the plate before welding was 70° F. The transversely welded specimen serves only to show whether or not the weld metal is stronger than the base metal. Measurement of elongations between gage points across the joint are of limited value because the materials between the gage points are of many different characteristics. Ductility of the joint as a whole is determined from the longitudinally welded specimen. It will be seen in Table 9 that E12015-welded specimens with the reinforcements removed broke in the base metal, and 100% joint efficiency was developed. Full strength was also obtained in the inert-gas-welded joint with the reinforcements removed. A somewhat lower joint efficiency obtained in E9015 welded joints with the reinforcements removed. In Table 10 a comparison of the results from the welded specimens with those from unwelded specimens shows that the percent elongations in the welded

Table 10—Results of Tests of Longitudinally Butt-Welded Tension Specimens of "T-1" Steel

Electrode type	*Plate thickness, in.*	*Heat No.*	*Condition of weld*	*Yield strength (0.2% offset), psi**	*Tensile strength, psi**	*Elong. in 8 in., %**	*Elong. in 2 in., %**
E12015	1/2	36S462	As-welded	102,900	120,200	11.2	
E12015	1	29S144	As-welded	101,400	119,900	14.3	27.7
E9015	1	32U029	As-welded	102,200	112,400	14.7	32.3
E9015	1	32U029	Stress relieved (1100° F)	102,100	111,100	16.0	33.3
Inert-gas shielded	1/2	74L236	As-welded	100,800	119,400	9.6	24.0
None (base metal)	1/2	74L236	As-welded	115,000†	123,000†	11.0†	25.0†

* Average of three values.
† Typical value.

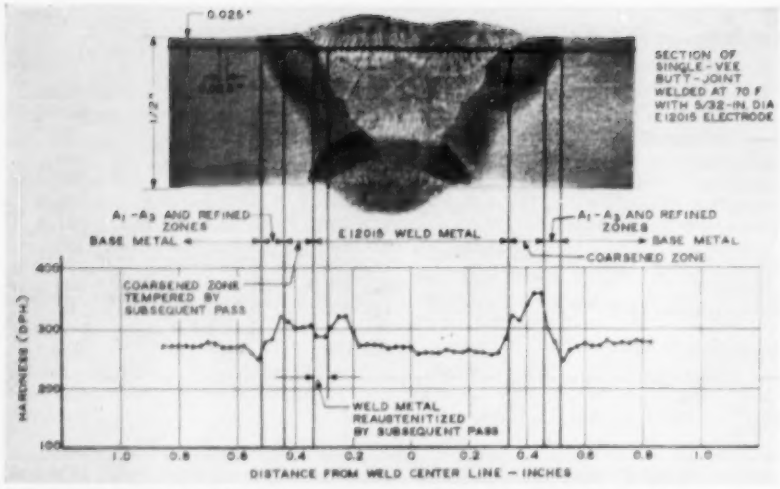
0.025"
SECTION OF SINGLE-VEE BUTT-JOINT WELDED AT 70 F WITH 5/32-IN DIA E12015 ELECTRODE
A1-A3 AND REFINED ZONES
BASE METAL
E12015 WELD METAL
A1-A3 AND REFINED ZONES
BASE METAL
COARSENED ZONE
COARSENED ZONE TEMPERED BY SUBSEQUENT PASS
HARDNESS (DPH)
WELD METAL REAUSTENITIZED BY SUBSEQUENT PASS
DISTANCE FROM WELD CENTER LINE — INCHES

Fig. 10 Results of hardness studies on a butt joint in $^1/_2$-in. "T-1" steel

specimens are in general somewhat higher than those of the comparable base metal specimens. Although the percent elongations for the inert-gas welded joints were lower than those of the comparable base metal specimens, the values are still good considering the high strength developed.

A Vickers hardness survey to determine the weld metal, heat-affected zone and base-metal hardnesses was made on a section cut from a joint in $^1/_2$-in. quenched and tempered "T-1" steel plate welded at 70° F with E12015 electrodes. All impressions were made with a $2^1/_2$-kg load. It will be seen in Fig. 10 that the peak hardness obtained in the coarsened zone was about 360 (DPH). A panorama showing the microstructures in the butt-welded specimen is given in Fig. 11. The microstructure in the coarsened zone, the location of maximum hardness in Fig. 10, was martensite and bainite. The oxygen-cut specimen shown in Fig. 11 will be discussed in detail in a later section.

The AWS-ASME Standard Qualification Procedure used for welding-procedure qualification and for welding-operator qualification requires that butt-welded specimens of a prescribed design be capable of bending around a $^3/_4$-in. radius mandrel using a guided bend jig. Historically, this guided-bend test was introduced in United States Navy specifications in the 1930's and the welded steels to which it was applied were usually the structural carbon steel grades welded with mild steel electrodes. The size of the mandrel and jig has remained unchanged even though the test is now applied to welded steels differing widely in strength and ductility. For reasons presented below, a suggested change in the guided-bend test as applied to joints in high-yield strength steels is given in Fig. 12. The illustrated specimens of quenched and tempered "T-1" steel were prepared as shown in Fig. 13 and then tested with the weld metal at the point of maximum bending.

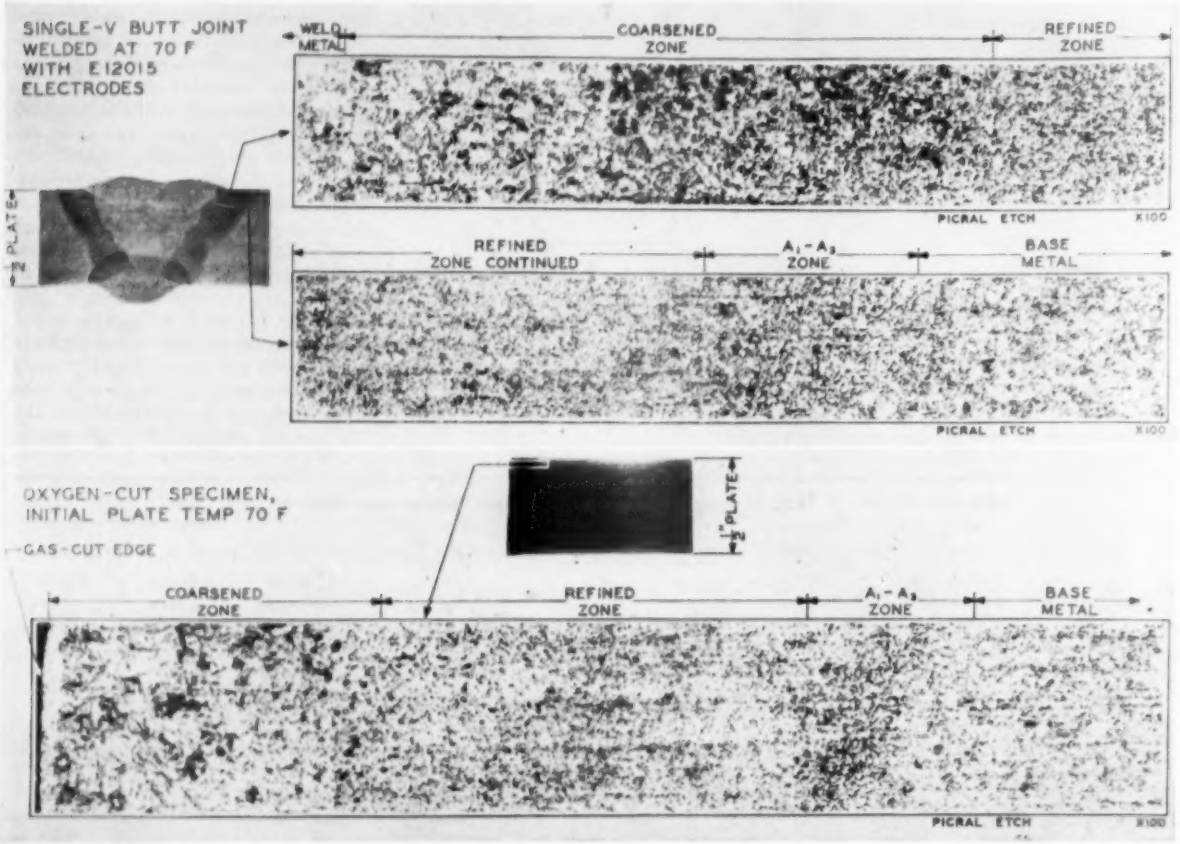

Fig. 11 Panoramas showing microstructures in a butt-welded specimen and in an oxygen-cut specimen—$^1/_2$-in. "T-1" steel

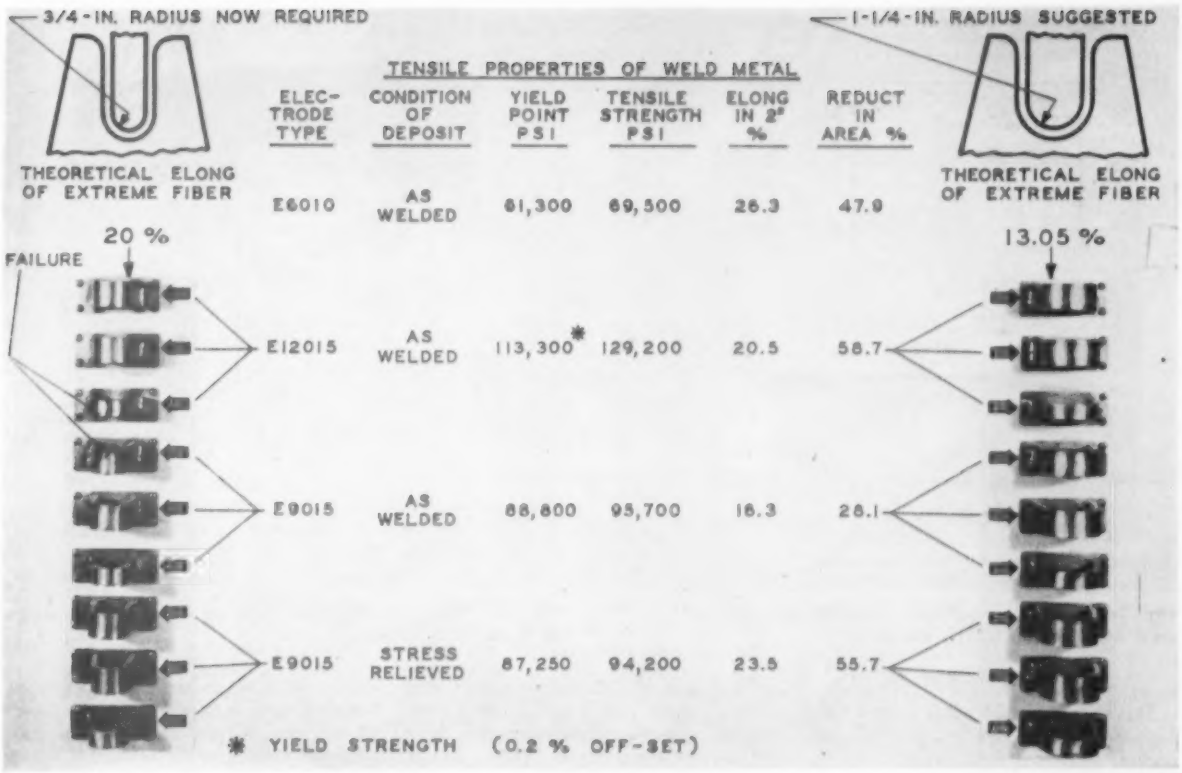

Fig. 12 Suggested change in guided-bend test

It may be noted in Fig. 12 that specimens welded with high-strength electrodes do not always pass the bend test when the standard $^3/_4$-in. radius mandrel is used. This size mandrel demands a theoretical elongation of 20% at the extreme fiber of the specimen. This value is nearly equal to the tensile elongation value for E12015 weld metal and less than the value for E9015 weld metal, as deposited. A typical tensile elongation value for E6010 weld metal is 26.3%. Since this value for mild steel weld metal is considerably larger than the 20% elongation demanded by the $^3/_4$-in. radius mandrel, no difficulty is experienced in passing the standard guided-bend test using speci-

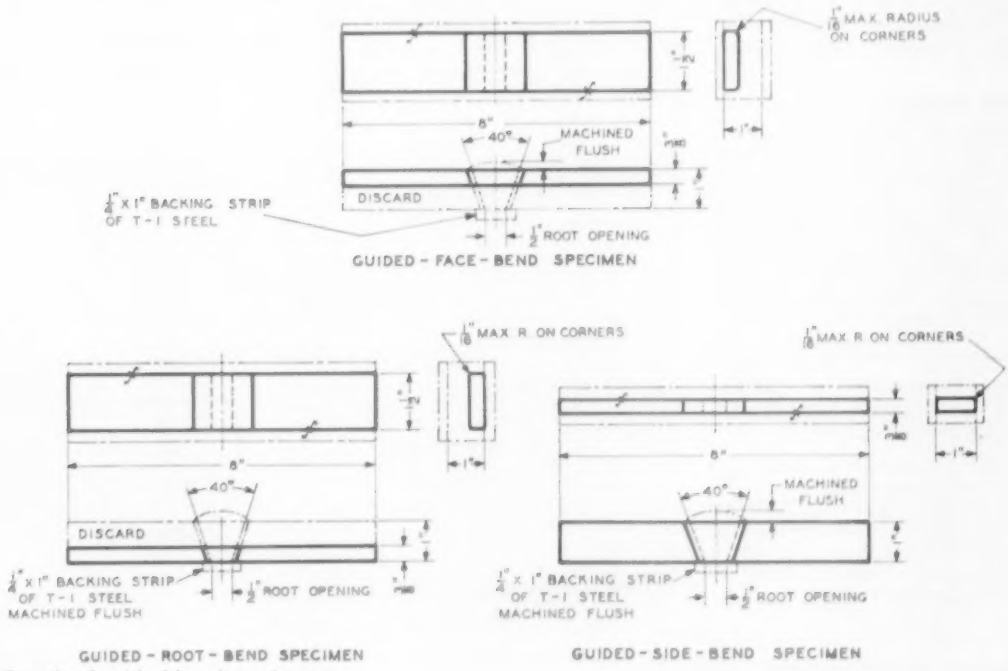

Fig. 13 Details of guided-bend specimens

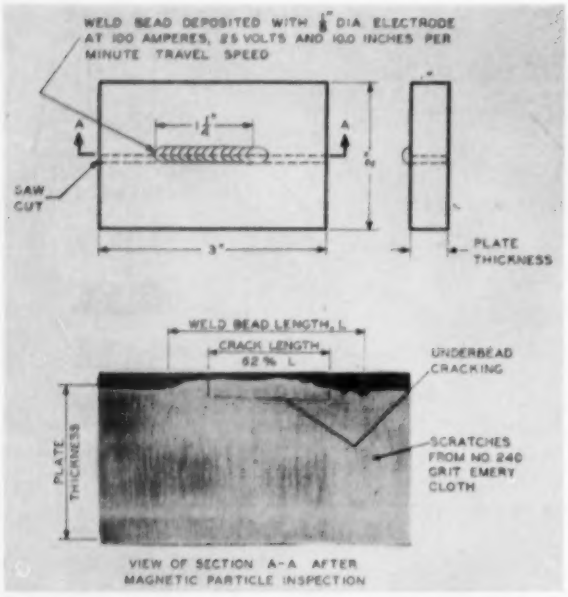
WELD BEAD DEPOSITED WITH 1/8" DIA ELECTRODE AT 100 AMPERES, 25 VOLTS AND 10.0 INCHES PER MINUTE TRAVEL SPEED
SAW CUT
3"
PLATE THICKNESS
WELD BEAD LENGTH, L
CRACK LENGTH 62 % L
UNDERBEAD CRACKING
PLATE THICKNESS
SCRATCHES FROM NO 240 GRIT EMERY CLOTH
VIEW OF SECTION A-A AFTER MAGNETIC PARTICLE INSPECTION

Fig. 14 Typical underbead cracking specimen of "T-1" steel welded with E6010 electrode

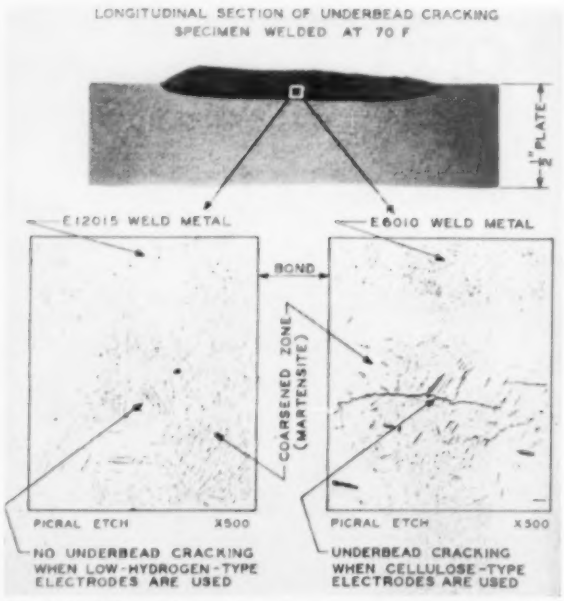
LONGITUDINAL SECTION OF UNDERBEAD CRACKING SPECIMEN WELDED AT 70 F
1/2" PLATE
E12015 WELD METAL
E6010 WELD METAL
BOND
COARSENED ZONE (MARTENSITE)
PICRAL ETCH X500
PICRAL ETCH X500
NO UNDERBEAD CRACKING WHEN LOW-HYDROGEN-TYPE ELECTRODES ARE USED
UNDERBEAD CRACKING WHEN CELLULOSE-TYPE ELECTRODES ARE USED

Fig. 15 Effect of electrode type on underbead cracking in 1/2-in. "T-1" steel

mens welded with E6010 electrodes.

It is suggested that joints welded with high-strength electrodes (E90XX or higher) be tested in a guided-bend jig having a mandrel radius of 1 1/4 in. Such a size mandrel demands a theoretical elongation of 13.05% in the test specimen and, in the case of weld metal having a minimum tensile elongation of 18% in 2 in., provides the same ratio between weld-metal tensile elongation and theoretical bend elongation as obtained from E6010 welded joints tested with a 3/4-in. radius mandrel. It may be noted in Fig. 12 that all specimens bent with the large mandrel passed the qualification test.

Underbead Cracking

Underbead cracking tests were made on 1/2-in-thick plates of quenched and tempered "T-1" steel from the pressure-vessel-test heat to determine the susceptibility of the steel to cracking during welding. For this test, bead-cracking specimens, as shown in Fig. 14, were made at each of three temperatures 0, 70 and 212° F, using E6010 electrodes; and at each of two temperatures 0 and 70° F, using E9015 and E12015 electrodes. These temperatures are those of the specimens before welding and of the bath in which the specimens are partially immersed during and after welding. The specimen preparation and examination were the same as described previously.[2] The cracking-test results are shown in Table 11. It will be seen for the specimens welded with the E6010 (cellulose covered) electrodes that a great amount of cracking occurs when the initial plate temperature is 70° F and a lesser amount occurs with plate temperatures of 0 and 212° F. However, all specimens welded with low-hydrogen (lime covered) electrodes show little or no underbead cracking. Accordingly, it is suggested that only low-hydrogen-type electrodes be used for the welding of "T-1" steel.

Typical photomicrographs showing the effect of electrode type on the susceptibility of "T-1" steel to underbead cracking in the coarsened heat-affected zone are given in Fig. 15. It will be seen in Fig. 16 that the peak hardness obtaining in the coarsened zone was about 420 (DPH). This hardness is unaffected by changes in type of electrode.

Notch Toughness

Throughout the development of "T-1" steel, the emphasis has been on tough-

Table 11—Susceptibility of 1/2-In.-Thick "T-1" Steel to Underbead Cracking (Heat 74L236)

Initial plate temp, ° F	Percent of Underbead Cracking* E6010 (cellulose type)	E12015 (low-hydrogen type)	E9015 (low-hydrogen type)
0	23	3	0
70	54	1	0
212	10	..	..

* Average of five tests.

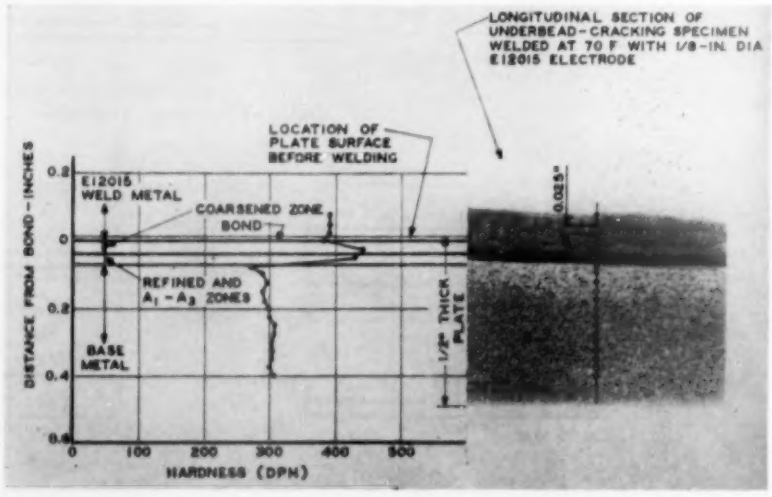

Fig. 16 Results of hardness studies on an underbead cracking specimen—1/2-in. "T-1" steel

ness. That good low-temperature toughness is achieved in the steel was demonstrated by the Charpy impact data in Table 3 and Fig. 2.

The effect of welding on the notch toughness of the steel was determined by Kinzel-type notch-bend tests on welded and unwelded plate in the quenched and tempered condition. Details of the welded plate specimen are shown in Fig. 17. The unwelded specimens were identical to the welded, except for the omission of the weld. The plate temperature prior to welding was 70° F, and after welding, the specimens were aged at 50–80° F for a minimum of three weeks. The specimens, in sets of about 25, were then bent at various temperatures with the weld bead in tension as shown in Fig. 17. In general, three specimens were tested at each temperature. The low-temperature bend testing equipment was the same as described previously.[2] A bath of Freon 11 and dry ice was used to obtain temperatures from room temperature to −100° F. Test temperatures below −100° F were obtained with a bath of Freon 12 cooled with liquid nitrogen.

The following information was determined for each specimen tested:

1. Total (maximum) angle of bend and angle of bend at maximum load. Obtained from deflection-angle curve.
2. Percent lateral contraction at a point $^1/_{32}$ in. below the base of the notch. Obtained by calipering the width of the specimen before and after testing.
3. Total energy absorbed. Obtained by planimetering load-deflection curve.
4. Fracture mode expressed as percent shear. Obtained by observation of fracture.

The data obtained were plotted on the basis of testing temperature, and envelopes were drawn around the plotted points to indicate the amount of scatter. The curves for $^1/_2$-in.-thick cross-rolled "T-1" steel welded with E12015 electrodes are shown in Fig. 18. The data are for specimens taken longitudinal to the final rolling direction of the plate.

In a previous paper[2] on "T-1" steel, lateral contraction at the customary level of one percent was used as the basis for selecting ductility-transition temperature by the Kinzel-type notch-bend test. This level has been used extensively by investigators when presenting comparable data for other steels.[4] The temperature at the one percent contraction level was selected at that time as the ductility-transition temperature because it corresponded to the middle of the temperature range in which all manifestations of ductility drop rapidly to an extremely low value.

Figure 19 shows four lateral contraction curves for $^1/_2$-in.-thick "T-1" steel welded with E12015 electrodes. The upper curves in Fig. 19 are for specimens taken from "cross-rolled" plate from heat 74L236. The lower curves are for specimens taken from "straightaway-rolled" plate from the same heat. Longitudinal specimens and transverse specimens were tested from each type of plate. It may be noted for the data obtained from the transverse specimens that the one percent contraction level is considerably above the middle of the range in which the contraction drops rapidly to a low value. Thus, the one percent level is not a consistently suitable criterion for the selection of the ductility-transition temperature by the Kinzel test. This fact has not been evident from published data on the Kinzel test because data have been presented only for specimens taken longitudinal to the rolling direction of the plates studied.

To determine ductility-transition temperatures, the middle of the temperature range in which the total angle of bend drops rapidly to a low value was chosen. This is the temperature at the middle of the "scatter band" in the angle of bend curve in Fig. 18. It will be seen in Fig. 20 for cross-rolled plate that the ductility-transition temperature, regardless of specimen orientation, corresponds to the temperature at which practically the last vestige of ductility de-

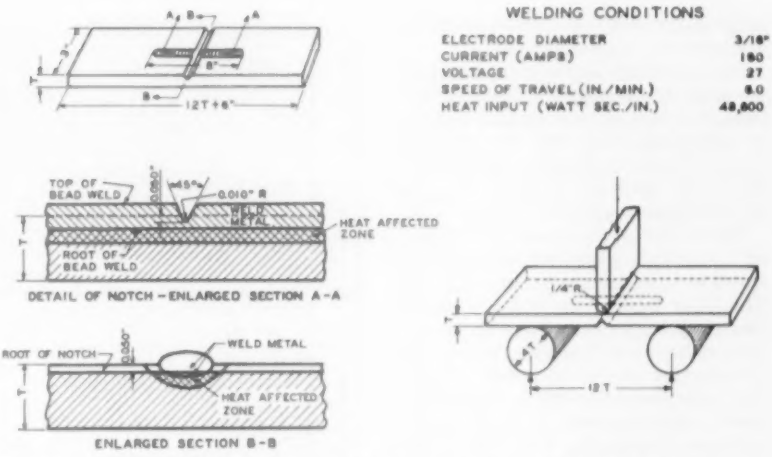

Fig. 17 Details of Kinzel-type notch-bend specimen and of testing jig

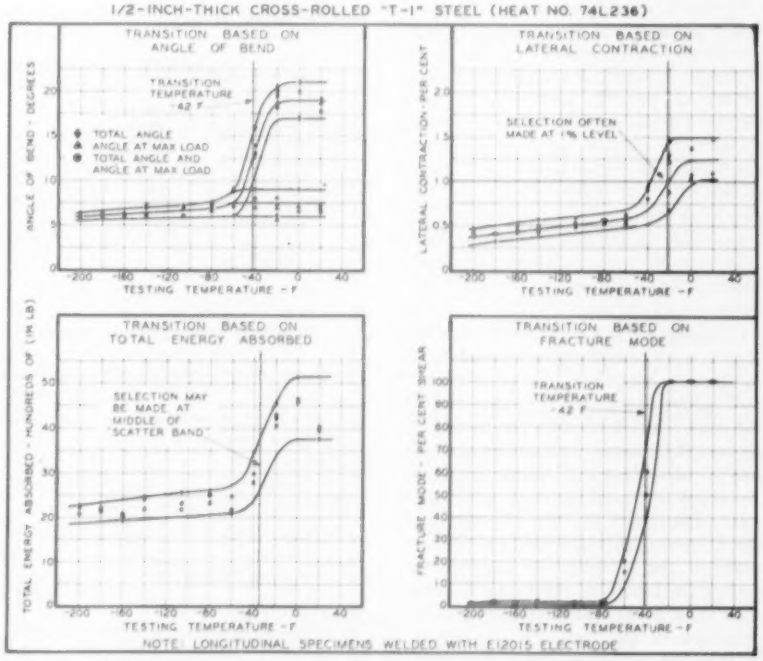
1/2-INCH-THICK CROSS-ROLLED "T-1" STEEL (HEAT NO. 74L236)
TRANSITION BASED ON ANGLE OF BEND
TRANSITION TEMPERATURE -42 F
TOTAL ANGLE
ANGLE AT MAX LOAD
TOTAL ANGLE AND ANGLE AT MAX LOAD
ANGLE OF BEND - DEGREES
TESTING TEMPERATURE - F
TRANSITION BASED ON LATERAL CONTRACTION
SELECTION OFTEN MADE AT 1% LEVEL
LATERAL CONTRACTION - PER CENT
TRANSITION BASED ON TOTAL ENERGY ABSORBED
SELECTION MAY BE MADE AT MIDDLE OF "SCATTER BAND"
TOTAL ENERGY ABSORBED - HUNDREDS OF (IN LB)
TRANSITION BASED ON FRACTURE MODE
TRANSITION TEMPERATURE -42 F
FRACTURE MODE - PER CENT SHEAR
NOTE: LONGITUDINAL SPECIMENS WELDED WITH E12015 ELECTRODE

Fig. 18 Measurements and transition-temperature-selection criteria in Kinzel-type notch-bend test

parts and completely brittle failure is imminent.

The fracture-appearance-transition temperature was also determined. This transition temperature as used here is the temperature at which 50% of the fracture occurs in the shear mode, i.e., 50% of the fracture is fibrous. A typical curve is shown in Fig. 18.

The results of notch toughness studies on $^1/_2$-in.-thick plate from the pressure-vessel-test heat are given in Table 12. The table includes data for straightaway-rolled plate as well as data for the cross-rolled plate that was used in the construction of the pressure vessels. The transition-temperature values for the straightaway-rolled plate agree with previously reported "T-1" steel data for straightaway-rolled plate from another heat.[3]

For the Kinzel test, it may be noted that the transition-temperature values for cross-rolled plate are considerably higher than those for the straightaway-rolled plate. In the Charpy-keyhole test, the ductility-transition-temperature values for cross-rolled plate are lower than those for straightaway-rolled plate. Thus, the Charpy-keyhole test as judged by the ductility-transition values rates the plates in a reverse order. This reversal does not obtain in the case of the fracture-appearance-transition values. In the Charpy V-notch test, the ductility-transition-temperature values are nearly equal and the fracture-appearance-transition-temperature values for the cross-rolled plate are considerably higher than those for the straightaway-rolled plate.

It may be noted from the Kinzel tests that welding raised the ductility-transition temperature either a small or a moderate amount in three cases (increases of 26, 12, and 38° F) and lowered this same temperature a moderate amount (21° F) in a fourth case. The extent and direction of the effect varied with the rolling history of the plate and the specimen orientation. A stress-relief treatment raised the ductility-transition temperature of welded longitudinal specimens from cross-rolled plate a small amount (12° F)

Included in Table 12 are data on drop-weight tests and bulge-explosion tests on $^1/_2$-in. quenched and tempered "T-1" steel plate from the pressure-vessel-test heat. The tests were conducted by the Naval Research Laboratory and details of the methods of conducting these two types of "crack-starter" tests have been described in the literature.[5,6] The drop-weight test is used by NRL to establish the temperature at which a steel plate loses its ability to develop more than a very small amount of deformation in the presence of a crack. In the test a $3^1/_2$-in. wide by 14-in. long plate specimen having a notched, brittle, hard-surfacing bead weld is loaded by the impact of a dropping weight. A stop is used to limit the total deformation to five degrees angle of bend. The ductility-transition temperature by the drop-weight test is the highest temperature at which the specimen is unable to withstand the controlled amount of bending without complete fracturing. The bulge-explosion test is used by NRL to establish the temperature range of transition from easy to difficult propagation of fractures (referred to in this paper as the "fracture - arrest - transition"). In the test, a 14- by 14-in. plate specimen having a notched, brittle, hard-surfacing bead weld is placed over a circular die and explosion loaded. The fracture-arrest-transition temperature by the bulge-explosion test is the middle of the temperature range in which the extent of plate fracture changes from complete fracture to partial fracture.

It may be noted in Table 12 that the

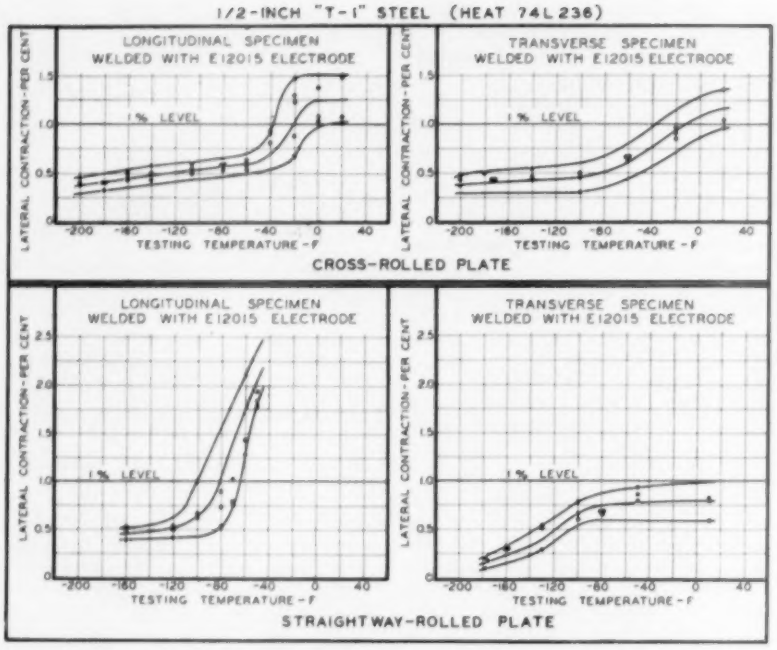
1/2-INCH "T-1" STEEL (HEAT 74L236)
LONGITUDINAL SPECIMEN WELDED WITH E12015 ELECTRODE
TRANSVERSE SPECIMEN WELDED WITH E12015 ELECTRODE
1% LEVEL
LATERAL CONTRACTION - PER CENT
TESTING TEMPERATURE - F
CROSS-ROLLED PLATE
STRAIGHTWAY-ROLLED PLATE

Fig. 19 Effect of type of rolling on lateral contraction in welded Kinzel specimen

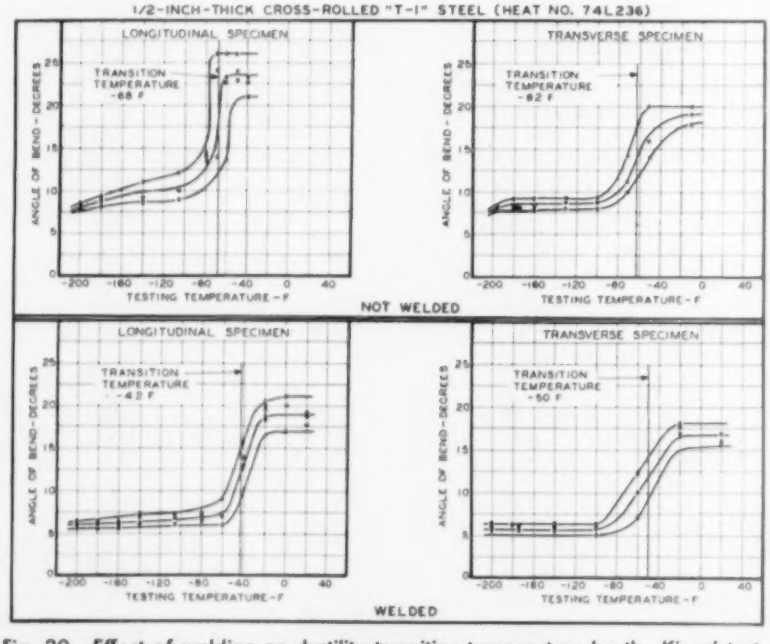

Fig. 20 Effect of welding on ductility-transition temperature by the Kinzel test

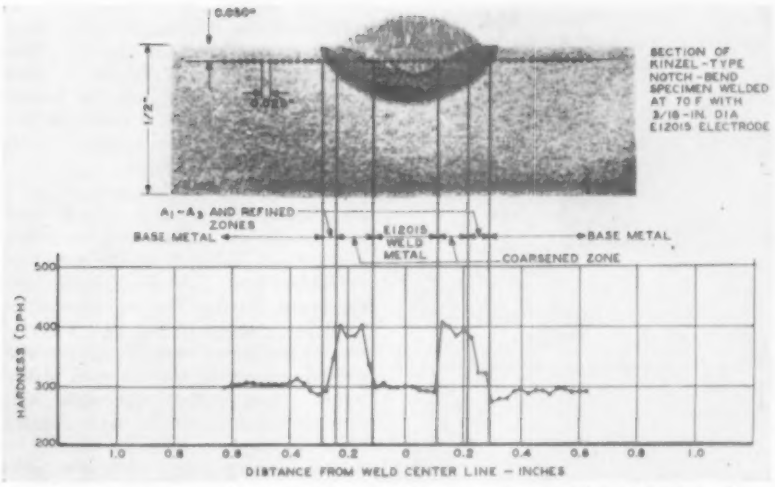

Fig. 21 Results of hardness studies on a Kinzel specimen—$^1/_2$-in. "T-1" steel

drop-weight test values for cross-rolled plate are only slightly higher than those for the straightaway-rolled plate. An additional observation which can be made from the cross-rolled-plate data in Table 12 is that the fracture-arrest-transition temperature by the bulge explosion test is nearly equal to the fracture-appearance-transition temperatures by the Kinzel test and by the Charpy V-notch test.

It should always be borne in mind that at the present time there is no absolute correlation between transition temperatures as obtained experimentally with the various types of specimen and the service performance of pressure vessel steels. However, the results obtained from the same types of tests on different steels should be very useful in establishing the relative merit of the steels.

A typical hardness survey for a Kinzel-type notch-bend specimen of $^1/_2$-in. quenched and tempered "T-1" steel welded at 70° F with E12015 electrodes is shown in Fig. 21. The survey is located on the transverse section at a distance of 0.050 in. below the plate surface, the same distance as the root of the notch in an unsectioned specimen. The peak hardness was approximately 400 (DPH). Photomicrographs of the weld metal and the coarsened zone microstructures are shown in Fig. 22.

Fatigue Tests

The fatigue properties of quenched and tempered "T-1" steel have been determined using polished rotating-beam fatigue specimens, pulsating-tension fatigue specimens and reverse-

Table 12—Summary of Notch-Toughness Test Data on $^1/_2$-In. "T-1" Steel Plate (Heat No. 747236)

Test	*Type of plate*	*Orientation of spec.*	*Condition of spec.*	*Ductility-transition temp., ° F**	*Fracture-appearance transition temp., ° F**
Charpy impact (keyhole notch)	Cross rolled	Long.	Unwelded	−212	−120
Charpy impact (keyhole notch)	Cross rolled	Trans.	Unwelded	−205	−115
Charpy impact (keyhole notch)	Straightaway rolled	Long.	Unwelded	−171	−157
Charpy impact (keyhole notch)	Straightaway rolled	Trans.	Unwelded	−179	−138
Charpy impact (V-notch)	Cross rolled	Long.	Unwelded	−183	− 50
Charpy impact (V-notch)	Cross rolled	Trans.	Unwelded	−180	− 45
Charpy impact (V-notch)	Straightaway rolled	Long.	Unwelded	−178	− 90
Charpy impact (V-notch)	Straightaway rolled	Trans.	Unwelded	...	...
Kinzel notch-bend	Cross rolled	Long.	Unwelded	− 68	− 67
Kinzel notch-bend	Cross rolled	Trans.	Unwelded	− 62	− 59
Kinzel notch-bend	Straightaway rolled	Long.	Unwelded	−130	−128
Kinzel notch-bend	Straightaway rolled	Trans.	Unwelded	− 91	− 91
Kinzel notch-bend	Cross rolled	Long.	Welded†	− 42	− 42
Kinzel notch bend	Cross rolled	Trans.	Welded†	− 50	− 47
Kinzel notch-bend	Straightaway rolled	Long.	Welded†	− 92	− 89
Kinzel notch-bend	Straightaway rolled	Trans.	Welded†	−112	−112
Kinzel notch-bend	Cross rolled	Long.	Welded and stress relieved‡	− 30	− 27
NRL drop-weight	Cross rolled	Long.	Welded§	− 80	...
NRL drop-weight	Cross rolled	Trans.	Welded§	− 80	...
NRL drop-weight	Straightaway rolled	Long.	Welded§	− 90	...
NRL drop-weight	Straightaway rolled	Trans.	Welded§	− 90	...
NRL bulge-explosion	Cross rolled	...	Welded§	...	− 50**

* Notes on Selection of Transition Temperature:

Test	*Ductility-transition temp. at*	*Fracture-Appearance-transition temp. selected at*
Charpy impact (keyhole notch)	Middle of band	50% shear
Charpy impact (V-notch)	10 ft-lb	50% shear
Kinzel notch-bend	Middle of band	50% shear
NRL drop-weight	Total failure at 5° angle	...
NRL bulge-explosion	...	Failure across plate**

† E12015 electrodes.
‡ E9015 electrodes; Stress relieved at 1100° F for 1 hr.
§ Hard facing electrodes.
** Fracture-arrest-transition temperature rather than fracture-appearance-transition temperature.

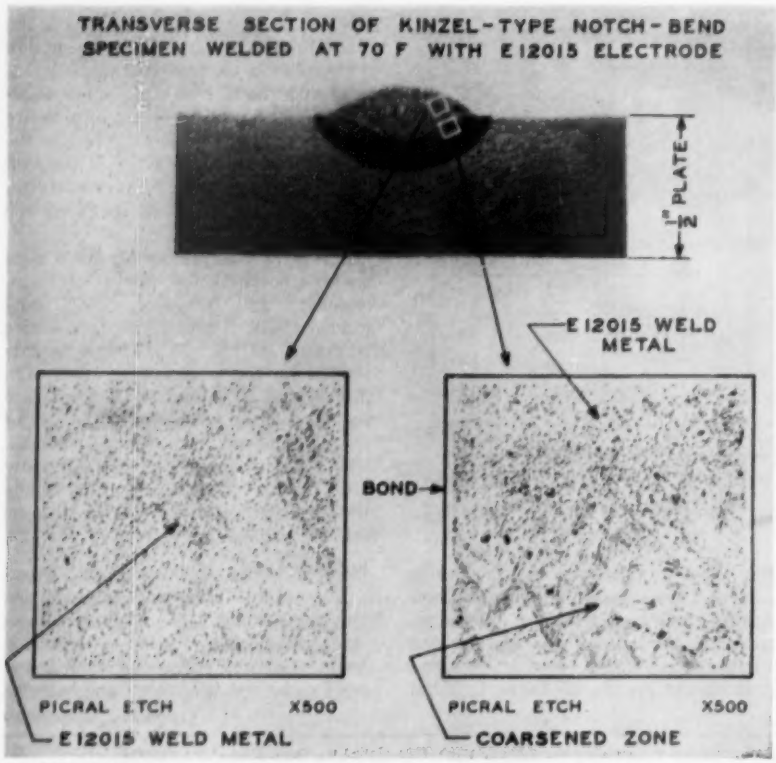

Fig. 22 Microstructure of weld metal and of coarsened zone of Kinzel specimen —1/2-in. "T-1" steel

bending fatigue specimens. The results from the polished rotating beam specimens are given in Fig. 23. The endurance limit of 67,000 psi, a value which is about 57% of the tensile strength of the material tested, is important only for comparison with similar data for other steels.

The pulsating-tension fatigue tests and the reverse-bending fatigue tests were made on welded and unwelded plate, 1/2-in. thick, from the pressure-vessel-test heat. Details of the welded specimens having the reinforcements in place are shown in Fig. 24. The unwelded specimens were identical to the welded, except for the omission of the weld. Shot-peened specimens and stress-relieved specimens were studied also. In addition, a limited number of reverse-bending fatigue tests were made on welded specimens similar to the bending-fatigue specimen shown in Fig. 24, except for removal of the weld reinforcements. The types and various conditions of welded and unwelded plate specimens used in the fatigue studies are summarized in Table 13. The intensity of peening used for the shot-peened specimens was measured by means of an Almen gage and an "Almen C" test strip. The deflection of the test strip was taken as a measure of the intensity of peening. The conditions used are given at the bottom of Table 13.

Table 13—Types and Conditions of Fatigue Specimens Prepared from 1/2-In. "T-1" Steel Plate (Heat No. 74L236)

	Condition of specimen				
Type of specimen	*Welding*	*Reinforcement*	*Stress relief*	*Peening*	*Surface*
Pulsating tension	None	....	None	No	Original surface complete with mill and heat-treating scale
Pulsating tension	None	In place	None	Yes*	Original surface, as-peened
Pulsating tension	E12015 welded	In place	None	No	Original surface, as-welded
Pulsating tension	E12015 welded	In place	None	Yes*	Original surface, welded and peened
Pulsating tension	E9015 welded	In place	1100° F for 1 hr	No	Original surface, welded and heat treated
Pulsating tension	E9015 welded	In place	1100° F for 1 hr	Yes*	Original surface, welded, heat treated and peened
Reverse bending	None	....	None	No	Original surface complete with mill scale and heat-treating scale
Reverse bending	None	....	None	No	Ground with surface grinder
Reverse bending	None	....	1100° F for 1 hr	No	Original surface, heat treated
Reverse bending	E12015 welded	In place	None	No	Original surface, as-welded
Reverse bending	E12015 welded	Removed	None	No	Ground with No. 240 grit emery-cloth-drum sander
Reverse bending	E9015 welded	In place	1100° F for 1 hr	No	Original surface, welded and heat treated
Reverse bending	E9015 welded	Removed	1100° F for 1 hr	No	Ground with No. 240 grit emery-cloth-drum sander, heat treated

* Technique:
Shot—No. 54 cut wire (SAE 1065 MB wire, 0.054 in. diam, 0.054 in. long)
Air pressure ..... 90 psi
Nozzle from work ..... 18 in.
Dwell (exposure) ..... 24 min/sq-ft
Almen gage deflection ..... 0.010 to 0.012 in.

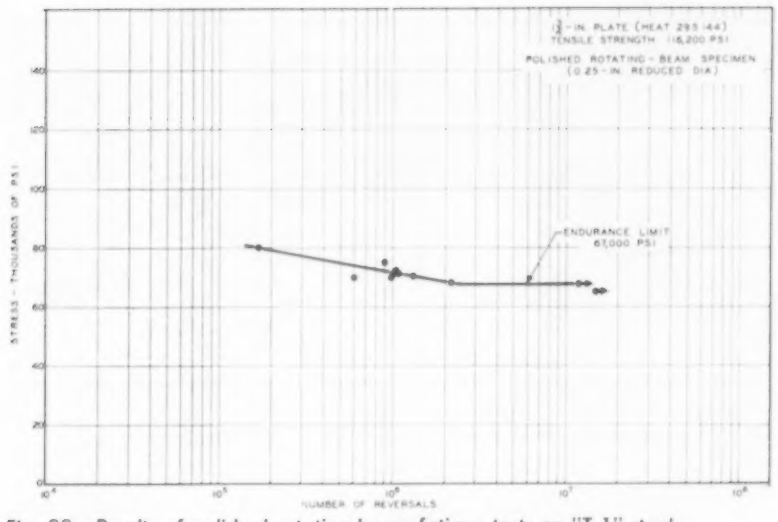

Fig. 23 Results of polished rotating-beam fatigue tests on "T-1" steel

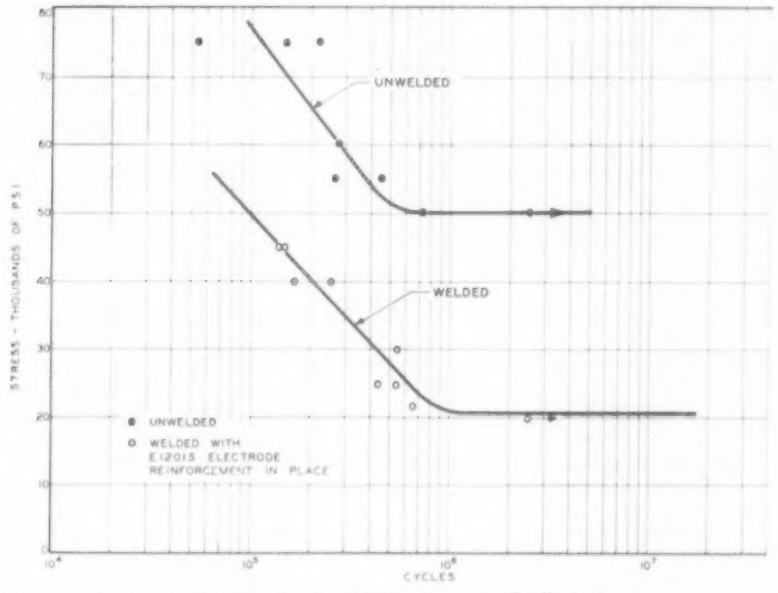

Fig. 25 Results of pulsating-tension fatigue tests on "T-1" steel

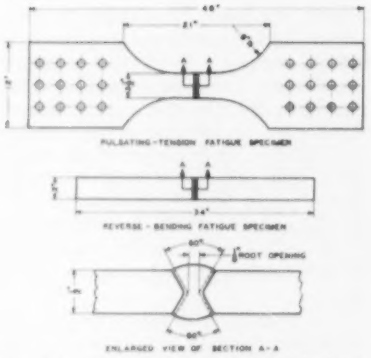

Fig. 24 Details of butt-welded fatigue specimens

The pulsating-tension fatigue tests were performed at the University of Illinois in the 200,000-lb capacity, Wilson lever-type fatigue testing machines described by Harris, Nordmark and Newmark.[7] The machines ran at a speed of 167 cpm. The reverse-bending fatigue tests were performed at the Applied Research Laboratory, United States Steel Corp., in a Rayflex resonant-frequency fatigue testing machine. These specimens were flexed at a rate of 8500 cpm.

The results of the pulsating-tension fatigue tests and the reverse-bending fatigue tests are summarized in Table 14. Typical pulsating-tension fatigue curves are shown in Fig. 25. Fatigue strengths were selected at the endurance limit, as is customarily done, and at 100,000 cycles. This latter criterion has been suggested by Dolan[8] to be significant for pressure vessel steels since few pressure vessels are called upon to endure cyclic loads more than 100,000 times. The loading is generally pulsating-tension rather than reverse-bending. Therefore, the remarks to follow will be confined to the pulsating-tension-fatigue-strength values selected at 100,000 cycles. The values for welded specimens, except those for specimens stress relieved and shot

Table 14—Summary of Fatigue Data for "T-1" Steel (½-In. Plate, Heat No. 74L236)

Condition of specimen					*Fatigue strength, psi*	
Type of loading	*Welding*	*Reinforcement*	*Stress relief*	*Other treatments*	*At 100,000 cycles*	*At endurance limit*
Pulsating tension	None		None	None	76,000	50,000
Pulsating tension	None		None	Peened	83,000	45,000
Pulsating tension	E12015 welded	In place	None	None	50,000	21,000
Pulsating tension	E12015 welded	In place	None	Peened	48,000	28,000
Pulsating tension	E9015 welded	In place	1100° F for 1 hr	None	44,000	26,000
Pulsating tension	E9015 welded	In place	1100° F for 1 hr	Peened	64,000	37,000
Reverse bending	None		None	None	54,000	36,000
Reverse bending	None		None	Polished	67,000	45,000
Reverse bending	None		1100° F for 1 hr	None	58,000	40,000
Reverse bending	E12015 welded	In place	None	None	35,000	14,000
Reverse bending	E12015 welded	Removed	None	None	44,000	28,000
Reverse bending	E9015 welded	In place	1100° F for 1 hr	None	50,000	16,000
Reverse bending	E9015 welded	Removed	1100° F for 1 hr	None	58,000	27,000

peened, are low. The fatigue test results indicate that the performance of a highly stressed "T-1" steel pressure vessel subject to fatigue might be improved by the use of a combination of stress relieving and shot peening following welding. It has been shown many times that removal of weld reinforcements raises fatigue strength a very significant amount; this is confirmed by the improvement in the performance of the specimens subjected to reverse bending.

In connection with the above remarks on fatigue, it should be remembered that few pressure vessel failures have been attributable to fatigue.[8,9] This observation is reasonable since the number of loading cycles sustained by many pressure vessels during their normal life can usually be counted in the thousands and fatigue would not be expected to be a problem with such vessels.

Residual Stress

Residual-stress measurements were made on a 36- by 36-in. unwelded plate and on a 24- by 48-in. butt-welded plate, both $^1/_2$-in.-thick "T-1" steel from the pressure-vessel-test heat. The butt joint along the longitudinal axis of the plate was welded at 70° F with E12015 electrodes. Neither the unwelded plate nor the welded plate was stress relieved. The residual stresses were determined by trepanning plugs containing resistance strain gages from three locations in the unwelded plate and from six locations in the welded plate. The highest measured stress in the unwelded plate was 15,470 psi compression, as shown in Fig. 26. The measured stresses in the welded plate are given in Figs. 27 and 28. The maximum measured stress in the weldment was 78,000 psi tension parallel to the weld at the weld center line, as shown in Fig. 27. At a distance approximately $1^1/_2$ in. on each side of the center line the stress parallel to the weld changed from tension to compression. The residual stresses transverse to the weld were low, as often obtains transverse to welds in other steels.

The practical significance of residual stresses approaching the 0.2% offset yield strength of "T-1" steel is discussed in a companion paper by Bibber.[1] It suffices to say at this time that residual stresses could affect the operation of a pressure vessel (1) when stress corrosion is of concern, (2) when precision machining is required and (3) when the welded vessel at its lowest operating temperature has insufficient ductility to endure local redistribution of stress. In the case of "T-1" steel, tests have shown that suitably designed and fabricated full-scale pressure vessels, welded and not stress relieved, are capable of enduring high pressures and severe impact at vessel temperatures approaching

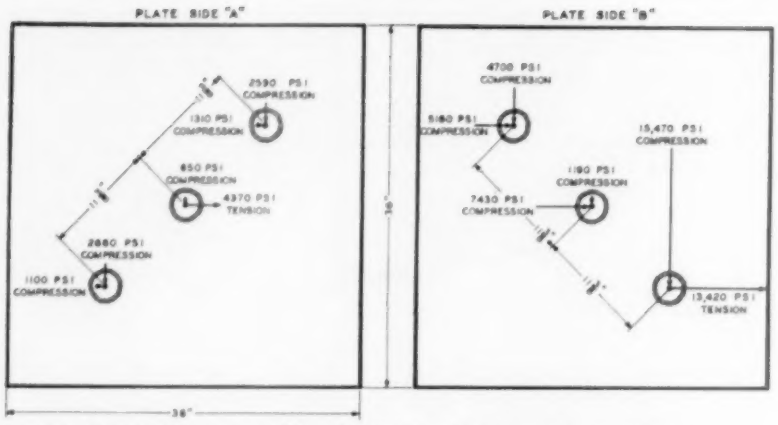

Fig. 26 Residual stresses in $^1/_2$-in. thick plate of "T-1" steel—unwelded

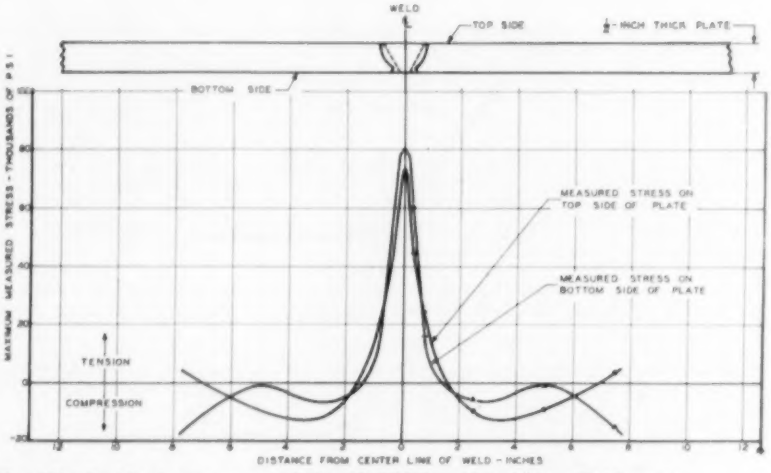

Fig. 27 Residual stresses parallel to E12015 weld in $^1/_2$-in. "T-1" steel

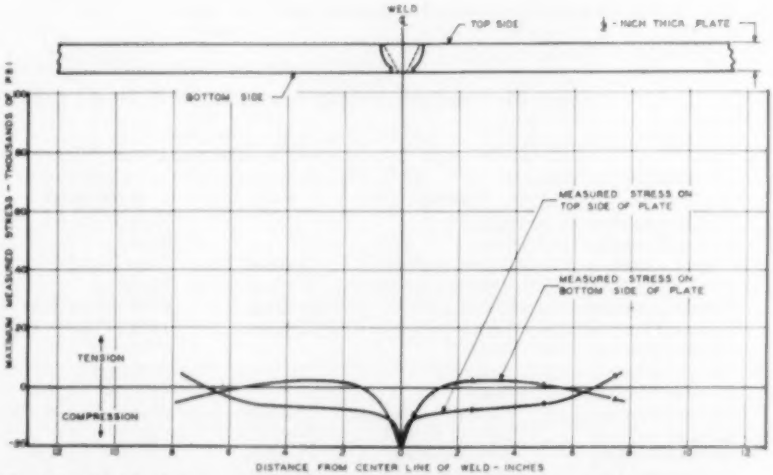

Fig. 28 Residual stresses transverse to E12015 weld in $^1/_2$-in. "T-1" steel

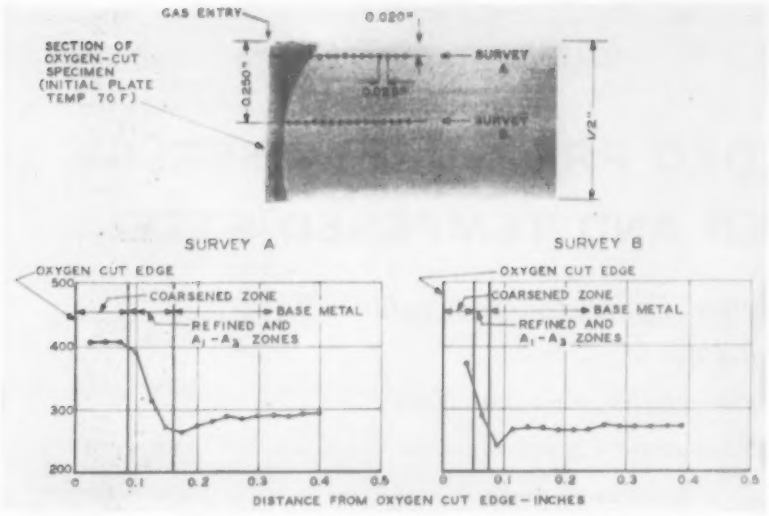

Fig. 29 Results of hardness studies on oxygen-cut specimen—$^1/_2$-in. "T-1" steel

−50° F. The performance of stress-relieved vessels was the same as that of the unstress-relieved vessels

Gas Cutting

The results of hardness studies on an oxygen-cut specimen of $^1/_2$-in.-thick quenched and tempered "T-1" steel are shown in Fig. 29. The plate temperature prior to cutting was 70° F. It may be noted that the heat-affected zone is approximately twice as extensive at the gas entry side of the plate as at the midthickness. This effect was caused by the impingement of the hot gases on the entry side of the plate. The peak hardness near this side (Survey A) was 410 (DPH), and the microstructure having this hardness was nearly all martensite. A panorama showing microstructures in the gas-cut specimen was given previously in Fig. 11. Previously published data[3] show excellent bend-test results for specimens gas-cut at 70° F, and tested at -98° F, thus demonstrating that high hardness resulting from gas cutting of quenched and tempered "T-1" steel does not preclude good performance. Gas cutting of as-rolled "T-1" steel plate is not recommended.

Summary

The test results described herein have shown that quenched and tempered "T-1" steel in the plate thickness range common for pressure vessel construction possesses a unique combination of properties and characteristics, namely:

1. Yield strength of at least 90,000 psi.
2. Good low-temperature notch toughness.
3. Ability to be welded without preheat or postheat, provided low-hydrogen type electrodes are used, and still remain tough as judged by notch-bend tests at low temperatures.
4. Low susceptibility to straining and to strain aging.
5. Uniformity of longitudinal tensile and transverse tensile properties.
6. Ability to be gas cut without preheat or postheat and still be deformed appreciably at low temperatures.

The fatigue strength (pulsating tension at 100,000 cycles) of welded "T-1" steel is low. However, the fatigue strength can be improved by a combination of stress relieving and peening and also by removal of weld reinforcements.

Acknowledgments

The author wishes to express his appreciation to L. C. Bibber and J. M. Hodge, Applied Research and Development, United States Steel Corp. and the various members of the Applied Research Laboratory and the Homestead District Works Laboratory, United States Steel Corp., who assisted in this investigation; to Professor W. H. Munse, University of Illinois, for conducting the pulsating-tension fatigue tests; to W. S. Pellini, Naval Research Laboratory, for conducting the drop-weight tests and the bulge-explosion tests; and to the Air Reduction Sales Co. for preparing the inert-gas-shielded metal-arc welds.

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Weldability of Steels

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DESIGN OF WELDED PRESSURE VESSELS USING QUENCHED AND TEMPERED STEEL

Allowable working stresses, design of appurtenances, welding in so far as it affects design, and use of material in conjunction with other engineering materials are the main issues covered in this paper

BY LEONARD P. ZICK

SYNOPSIS. Nine welded pressure vessels made of T-1 steel, a quenched and tempered material, were tested to destruction. These tests dramatically indicated the strength and toughness of this material under both static and impact loads at low temperature and at stresses considerably above the ultimate strength of most materials now listed in the Codes. These tests raise new and shed light on many old controversial issues pertaining to the design of pressure vessels.

Although one particular steel was used in the tests, many of the issues discussed apply more broadly to notch tough steels having high yield and ultimate strengths and high yield to ultimate strength ratios such as those being studied by the Pressure Vessel Research Committee. Economic considerations suggest the utilization of the greater elastic strength by permitting allowable design stresses greater than one-fourth of the ultimate strength.

The use of material having a minimum yield strength of 90,000 psi suggests design stresses greater than the yield of some carbon steels. This use therefore requires new thinking. Refinements of design details such as connections, junctions and attachments and increased inspection rule requirements may well be justified.

Introduction

The ASME Boiler and Pressure Vessel Code Committee first recognized the use of quenched and tempered high-strength steel by adopting in 1951 Code Case #1134. This Code Case permits the construction of small (5 cu ft maximum) seamless steel vessels at a working stress of 30,000 psi which is one-fourth of the minimum tensile strength of the material specified. This Code Case further requires streamlining the vessel (eliminating all possible stress raisers) and prohibits welding.

A new weldable high-strength quenched and tempered steel was also first reported in 1951.[1] Since that time a considerable amount of development work and testing has been done on this new material. This work is to be reported more fully in companion papers by W. D. Doty[2] and L. C. Bibber.[3]

To insure the suitability of this quenched and tempered material for use in welded pressure vessels, a series of tests* to destruction was carried out on nine full-size vessels of $^1/_2$-in.-thick high-strength quenched and tempered alloy steel. This testing and experience, which form a background for this paper, have been, to a large extent, based on one high-strength quenched and tempered steel called "T-1 constructional alloy steel".† Therefore, throughout the paper reference will be made simply to T-1 steel; however, many of the comments will apply to high-strength notch tough steels in general.

The Pressure Vessel Research Committee (PVRC) of the Welding Research Council (WRC), over the past few years, has sponsored extensive investigations of six selected high-strength steels[4] suitable for pressure vessels. Two of these steels were quenched and tempered material 1 in. thick and each had a minimum specified yield of 90,000 psi. PVRC has recently instigated a continuing program on heavy sections of three of the originally selected high-strength steels. The steels which will be further tested in 4-in. thickness are: A-302 steel having a minimum specified yield of 50,000 psi, Navy "G" steel having a minimum specified yield of 70,000 psi and T-1 steel having a minimum specified yield of 90,000 psi.

In all these PVRC investigations A-201 steel was also studied as a basis of comparison.

The laboratory tests sponsored by PVRC and full-size test to destruction of T-1 material attest to its weldability, exceptional notch toughness and high tensile properties.

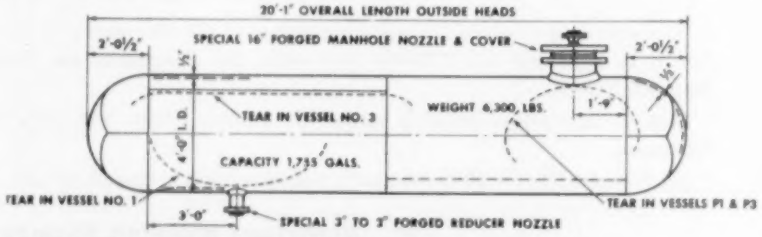
* These tests, known as "Operation T-1," were conducted jointly by the U. S. Steel Corp., and the Chicago Bridge & Iron Co. at the latter's plant in Birmingham, Ala.

† A patented product of the U. S. Steel Corp.

Test Results of T-1 Vessels

Nine vessels were tested to destruction. Eight of these vessels had inserted type nozzles made from forged T-1 steel. The general proportions of these vessels are shown in Fig. 1.

After completing the regular series of tests it was decided to test the built-up type of reinforced opening. A ninth vessel was fabricated from the unfailed halves of two original test vessels. Identical built-up nozzles were installed—one in each half. One half (5A) was welded with E9015 electrodes and stress


20'-1" OVERALL LENGTH OUTSIDE HEADS
2'-0½"
SPECIAL 16" FORGED MANHOLE NOZZLE & COVER
2'-0½"
TEAR IN VESSEL NO. 3
WEIGHT 6,300 LBS.
CAPACITY 1,735 GALS.
TEAR IN VESSEL NO. 1
TEAR IN VESSELS P1 & P3
3'-0"
SPECIAL 3" TO 3" FORGED REDUCER NOZZLE

Burst test vessels: P1 and No. 1 as welded; P3 and No. 3 stress relieved.
Drop test vessels: P2 and No. 2 as welded; P4 and No. 4 stress relieved.

Fig. 1 T-1 test vessels with inserted forged nozzles

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Presented at 1955 AWS National Spring Meeting held in Kansas City, Mo., June 7-10, 1955.

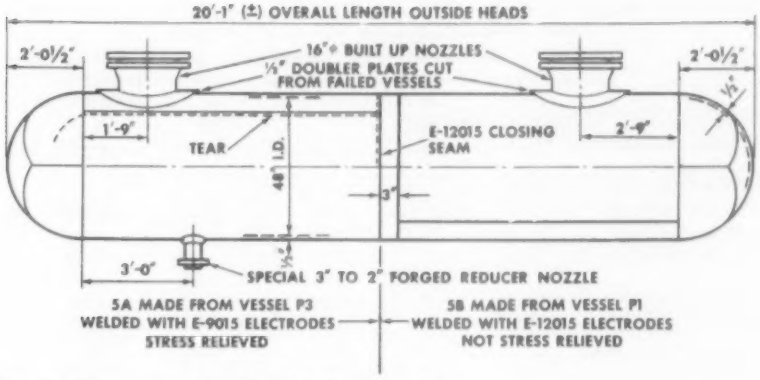


Fig. 2 T-1 test vessel No. 5 with built-up nozzles

relieved after welding. The other half (5B) was welded with E12015 electrodes and was then welded to the stress-relieved half-vessel with the same electrode. The details of the additional vessel (Nos. 5A and B) are shown in Fig. 2.

Four of the original eight vessels and the additional vessel with the built-up nozzles were tested by a hydrostatic burst test at subzero temperatures. Figure 3 shows a typical burst test setup. Vessel No. 5 is shown covered with frost after being refrigerated to −47° F. Internal pressure was applied to each of the five vessels until they burst. Failure occurred as a ductile tear in each case but the locations varied. Two of the stress-relieved vessels failed by tearing almost the entire length of the main longitudinal butt weld. This failure in Vessel No. 5 is shown in Fig. 4. The general locations of the other tears are indicated in Fig. 1. The results of these tests are summarized in Table 1.

The other four vessels were tested by a unique method of drop tests. The vessel was first refrigerated to subzero temperatures and then the internal pressure was raised until the roundabout stress in the cylindrical shell of the vessel was near the yield strength, 90,000 psi. By dropping a 13-ton weight on the center of the vessel, the internal pressure was instantaneously raised to pressures considerably higher than those required to fail the vessel under static conditions.

At least three drops were made on each test vessel. These were successive drops from 52 ft, then 73 ft and finally 101 ft. Figure 5 shows a typical vessel after successive drops from 52 ft and 73 ft heights without rupture.

The cross section at the center under the impact of the weight was actually reduced; however, the cylindrical shell on either side was stretched plastically by the resulting pressure wave without rupture. After the impact, the internal pressure was reduced substantially because of the increase in volume of the vessel.

The increase in girth without rupture in the region of the fittings and longitudinal welds exceeded that which caused failure under static conditions. Although discontinuities were held to a minimum in the design of these vessels, each failure in the burst tests started adjacent to a fitting or a weld. Therefore, the drop tests indicated that impact loads causing above-ultimate static stress levels at subzero temperature did not cause premature failure in these vessels.

The internal pressures for various drops are summarized in Table 2. Failure was finally caused in the shell adjacent to the center circumferential weld. The combined longitudinal stress due to internal pressure plus the longitudinal bending stress caused by the impact, was finally great enough to cause a shear failure to start at the bottom center point of the vessel and to continue until the vessel was torn into halves.

Figure 6 shows a longitudinal view of the $^1/_2$ in. T-1 shell under the striking saddle after the drop from a height of 101 ft. It is significant that after each impact the circumferential stress was hydrostatically raised to the yield strength and the longitudinal stress to

Table 1—Results of Burst Tests of T-1 Vessels

	Not stress relieved — *E12015 electrode*		*Stress relieved* — *E9015 electrode*		
Vessel No.	P1	No. 1	P3	No. 3	No. 5A*
Pressure (psi) at failure	2,740	2,850	2,690	2,850	2,770
Nominal stress at failure (psi)	131,600	136,800	129,200	136,800	133,000
Location of failure	Shell near 16-in. nozzle	Shell near 3-in. nozzle	Shell near 16-in. nozzle	Long. seam in shell	Long. seam in shell
Type of failure	100%, 45° shear	100%, 45° shear	100%, 45° shear	Over 95%, 45° shear	Over 95%, 45° shear
Max. increase in girth	3 in.	$5^5/_{16}$ in.	$2^3/_8$ in.	1 in.	$1^3/_4$ in.

* As-welded half of vessel marked 5B withstood this pressure without failure.

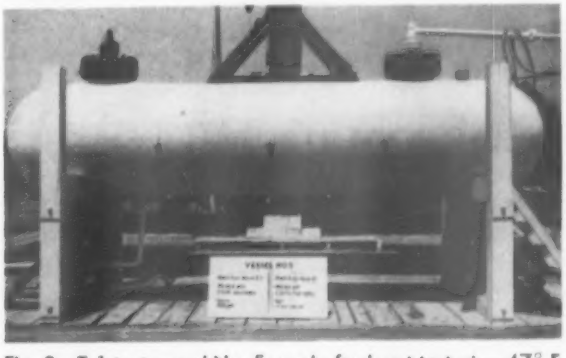


Fig. 3 T-1 test vessel No. 5 ready for burst test at −47° F

Fig. 4 Failure in the stress-relieved (E9015 weld) portion of vessel No. 5 at 2770 psi internal pressure

half this amount or 45,000 psi. The distortion was caused by the combined effect of local bending due to the pressure of the impact at the edge of the striking saddle combined with axial compressive stresses due to the longitudinal bending of the vessel at impact.

Vessel Experience

A number of pressure vessels of T-1 material have been fabricated and more are contemplated. These vessels have been built under special conditions which did not require a Code stamp.

One of these vessels was a 9 ft $2^3/_4$-in. diameter sphere made of $^3/_4$-in. T-1 steel plate. This vessel has been used as a testing machine to test some fifty disks of structural steel each about 22 in. in diameter.[5] A brine refrigerated to 0° F is used to hydrostatically test each disk to failure. The failure is usually triggered by a purposely made flaw such as a weld crack across the center of the disk. The brittle character of the failure stops at the weld between the disk and the T-1 sphere. If the tear carries across the weld it changes to a *ductile* failure and stops in a matter of inches. The failed disk is removed by torch cutting. Any crack in the T-1 sphere is repaired and a new disk is welded in place from the outside only without a back-up bar.

Pressures as high as 1400 psi, which represents a stress of over 50,000 psi in the T-1 sphere, have been used to fail the disks. Some of the disks have been cycled as many as 1000 times at near critical stress levels without failure; failure has then been instigated by a higher static load.

The National Advisory Committee for Aeronautics has purchased a number of shop-built T-1 vessels and has on order at least one larger field erected vessel. One of these vessels was 10 ft in diameter by 51 ft tangent length. The cylindrical shell was $1^1/_4$-in. thick and the hemispherical ends were $^5/_8$ in. thick. The design was based upon the NACA Specifications which required a maximum design stress of 40% of the yield. The vessel was fully X-rayed, therefore a 90% joint efficiency was used giving a working stress of 40% × 90,000 × 90% = 32,400 psi. This vessel had fittings of the built-up type; however, special precautions were taken as will be discussed later.

In Cologne, Germany, a sphere 110.7 ft (33.75 meters) in diameter has been built for the storage of natural gas at about 6 atmospheres. The 1.18-in. (30 mm) shell is of HSB50 German steel which has a yield strength of over 50,000 psi and an ultimate strength of over 70,000 psi.

Similar gas spheres are contemplated for another foreign country. These spheres will be 110 ft 6 in. in diameter and of 0.73 in.-T-1 steel plates. The design basis is the same as that specified by the NACA. However, in this case the fittings will be of the insert type.

Code Design Stresses

The Unfired Pressure Vessel Code, Section VIII of the ASME Boiler and Pressure Vessel Code, has appendices which cover the approval of new materials and set the basis for establishing stress values for both ferrous and nonferrous materials. Figure 7 shows a comparison of working stress and yield stress as percentages of the ultimate strength for various materials for the atmospheric temperature range. Except that the last two high-strength steels are not as yet Code materials, the values are based upon the rules of the ASME Unfired Pressure Vessel Code.

From this it is apparent that for the atmospheric temperature range the nonferrous materials utilize design stresses which are set by the rule which specifies a maximum of two-thirds the yield strength of the material since this is less than one-fourth of the ultimate strength. Design stresses for the stainless materials are calculated the same by both Code rules; one specifies a maximum of $62^1/_2$% of the yield strength and the other specifies a maximum equal to one-fourth of the ultimate strength. The carbon and low-alloy steels have their design stresses set by the Code rule which specifies a maximum equal to one-quarter of the ultimate strength.

Almost everyone will agree that at some point above the yield strength the

Table 2—Results of Drop Test on T-1 Vessels

	Energy in millions of ft-lb	Internal pressure in psi — Not stress-relieved — E12015 electrode		Internal pressure in psi — Stress relieved — E9015 electrode	
Vessel No.	...	P2	2	P4	4
(Temp. of vessel)	...	(−39° F)	(−33° F)	(−34° F)	(−41° F)
Before 24-ft drop	...	1800			
After 24-ft drop	0.64	1560			
Before 52-ft drop	...	1560	1875	1880	1880
Max at impact	1.39	...*	2800	2910	...*
After 52-ft drop	...	1070	1230	...†	1320
Before 73-ft drop	...	1070	1860	1880	1860
Max at impact	1.95	...*	3200	...*	3200
After 73-ft drop	...	780	1090	1030	1060
Before 101-ft drop	...	780	1875	1820	1890
Max at impact	2.70	...*	3300	...*	3500
After 101-ft drop	...	Failure	Failure	Failure	Failure

* Not measured.
† Pressure gage line broke off.

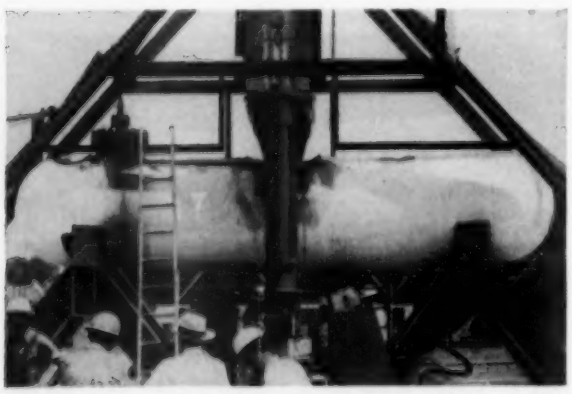


Fig. 5 Drop test vessel after impacts without rupture

Fig. 6 Typical drop test vessel failure showing distortion of $^1/_2$-in. shell under striking saddle

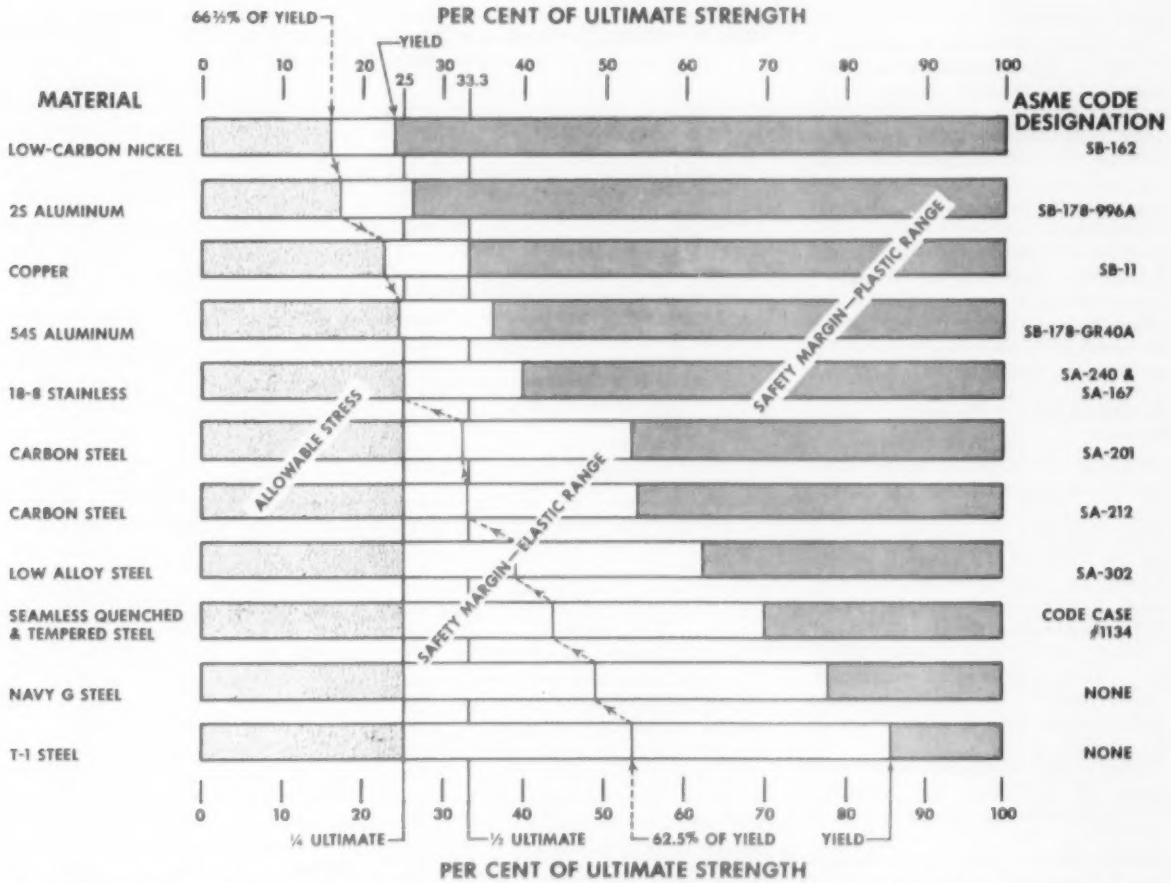
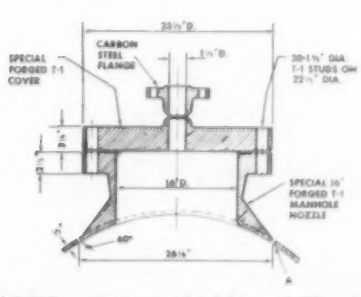


Fig. 7 Comparison of allowable stress and yield stress as a percentage of the ultimate strength for materials within atmospheric temperature range

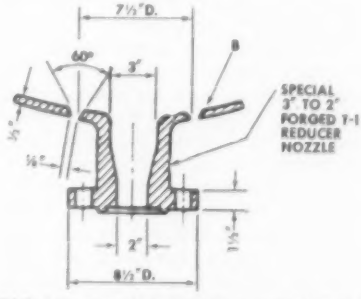
distortion will be great enough to represent failure. On the other hand, the PVRC results indicate that plastic fatigue strengths of the steels tested are more nearly associated with the ultimate strength than with the yield strength. Therefore, the double rule which includes the ultimate strength as well as the yield strength has merit; however, the percentages used do not seem consistent over the range covered by Fig. 7. The adoption of any lower factor of safety on the ultimate will not benefit the stainless steels or the nonferrous materials since they are already based upon a high percentage of the yield strength. The elastic margin of the high-strength steels is excessive by comparison. For T-1 steel, an overpressure 3.4 times the design pressure would be required before reaching the yield strength.

Low-Temperature Service

A survey[6] of brittle failures in welded steel structures other than ships points out the importance of notch toughness since practically all failures reported started at a notch and occurred in a brittle manner at stresses far below those predicted by ordinary tensile specimens. There is considerable evidence that many steels are susceptible to brittle failure at notches in comparatively low stress fields at temperatures of 40° F and lower. The PVRC tests indicate lower transition temperatures for all of the six steels tested than for the normalized A-201 steel. The quenched and tempered material had the lowest transition temperature of the group. A minimum temperature of −50° F. seems reasonable for *welded vessels* of T-1. This compares with A-201 plates ordered to A-300 which includes normalizing and guaranteed Charpy values of at least 15 ft-lb at −50° F for the A-201 plates

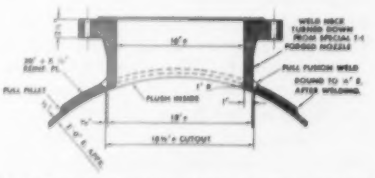


(a) Failure started at point A in vessels P1 and P3.



(b) Failure started at point B in vessel No. 1; failure started in longitudinal seam in vessel No. 3

Fig. 8 Nozzles of T-1 test vessels



(c) Built-up nozzles of vessel No. 5; failure started in longitudinal seam of shell.

before fabrication.

It is interesting to note the similarity of some of the properties when comparing T-1 steel with the stainless steels and the nonferrous materials which utilize design stresses based upon two-thirds of the yield strength. In addition to the similar low-temperature properties, the stress-strain curves do not have a definite yield point, so are based upon 0.2% offset. However, the stress-strain curve for the T-1 steel[3] shows a rather abrupt change in slope rather than the sweeping type curve associated with stainless steel and the nonferrous materials.

Suggested Higher Design Stresses

Some of the suggested design stress levels for T-1 steel are listed in Table 3. There are many reasons for the wide variation in the values listed and it is not the intent of this paper to specify one over the other for all designs. Economic considerations preclude the use of T-1 material for the run of the mill pressure vessels using present ASME rules. On the other hand, its use may well be justified for a special vessel at design stresses as low as one-fourth of the ultimate.

Design stresses based upon yield alone may well require more than a single temperature range since the yield strength at 650° F is approximately 10,000 psi lower than at room temperature. The ultimate strength, on the other hand, is nearly constant. The higher design basis will also require special design and fabrication refinements to eliminate stress raisers especially where fatigue is a factor.

The laboratory tests of the PVRC program indicate that the fatigue strength of T-1 exceeded that of all the other materials tested including A-201, but this increase was not always in direct proportion to its increase in strength. None of the steels failed at 100,000 cycles unless the loading approached the yield strength. Therefore, fatigue failures should not be a problem for pressure vessels in normal service designed at one-half of the yield or less provided that stress raisers are kept under a severity factor of 2. However, many of the constructions permitted by the Codes result in stress raisers in excess of 2

Past experience indicates that stress raisers are smoothed out to some extent by the initial overload test. The biaxial plate bending fatigue test[4] emphasizes this point for the more ductile A-201 material. Distortion at the first loading changed both the geometry and the stress system through the thickness. The correlation of laboratory fatigue tests at or above the yield strength with actual service conditions of pressure vessels is still to be resolved. The fact that all the steels tested gave good results should give the designer confidence in the present Code design criteria.

Table 3—Various Tension Design Stresses Suggested for T-1 Steel for Temperatures Between —50 and 650° F

	Nominal stress, psi	*—Complete X-ray— Jt. eff.*	*Design stress, psi*
Inquiry to ASME Boiler Code			
50% of min yield strength† (90,000 psi)	= 45,000	(90%)	= 40,500
Suggested for critical designs*			
1/3 of min ultimate strength (105,000 psi)	= 35,000	(100%)	= 35,000
NACA Specifications			
40% of min yield (90,000 psi)	= 36,000	(90%)	= 32,400
API low-pressure rules applied to T-1			
0.5 psi to 5 psi gas pressure at top, smaller of 60% of min yield or 1/3 of min ultimate (105,000 psi)	= 35,000	(95%)	= 33,250
5 psi to 15 psi gas pressure at top, 1/4 of min ultimate (105,000 psi)	= 26,250	(95%)	= 24,950
1952 ASME Boiler Code Rules applied to T-1 (also inquiry)			
Smaller of 62 5% of min yield or 1/4 of min ultimate (105,000 psi)	= 26,250	(90%)	= 23,650

* Suggested by engineers of M. W. Kellogg Co.[7] for vessels designed and inspected far more critically than now required by Code Rules.

† 0 2% offset.

For many applications economic considerations require the utilization of a greater portion of the available elastic strength which means the use of design stresses in excess of one-fourth of the ultimate. Therefore, the design stress level for such a case using T-1 steel exceeds the yield strength of mild steel. This fact alone requires special consideration by the designer. However, where the use of T-1 material is economically justified, special design consideration regarding the welding, the heads, the fittings, the supports and other attachments is also justified. It could also be said that streamlined vessels of any material that are designed without discontinuities warrant the use of higher design stresses.

Reinforced Openings

One of the main reasons for a large factor of safety is to provide for points such as the reinforced openings where stress concentrations might occur. The nozzles which gave remarkable performance during the T-1 tests are shown in Fig. 8. All of the butt welds were completely X-rayed, and the welds of the built-up nozzles were inspected with the magnetic particle method.

Although failure started adjacent to the 16-in. diameter forged nozzle in one stress-relieved vessel and one as-welded vessel, it is believed that the stress level was so high that failure could be triggered by almost any minor weakness. This is substantiated by the fact that vessel P3 failed adjacent to the nozzle at a pressure of 2690 psi; whereas, after welding a built-up nozzle into the good half of vessel P3 and after stress relieving, the vessel failed in a longitudinal weld at a pressure of 2770 psi.

The typical details for the nozzles used on the NACA tank previously mentioned are shown in Fig. 9. In this case it was not possible to X-ray the welds so each pass was inspected with the magnetic particle method.

One of the important factors to consider in using the present Code rules with increased design stresses for material such as T-1 is that the Code permits the use of weaker materials as reinforcement The use of a 55,000-psi carbon steel neck with a T-1 reinforced shell should not be permitted because the carbon steel neck would be overstressed near the reinforced shell. That is, the neck would be stressed above the yield point each time the vessel reached the design stress. For this reason it is advisable to use T-1 material for a por-

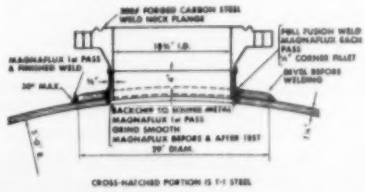


Fig. 9 Built-up nozzle used for T-1 pressure vessel

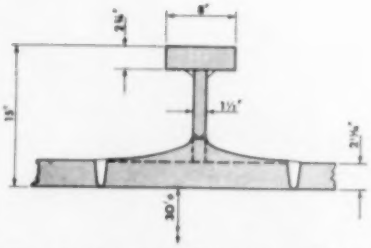


Fig. 10 Penstock fillet insert could be replaced with higher strength shell of uniform thickness

tion of the neck adjacent to the T-1 shell as shown in Fig. 9. The neck should extend beyond the reinforcement area at least a distance equal to the square root of the product of the neck radius times the actual thickness of the neck. At this point, a weaker material such as a carbon steel welding neck can be welded safely to the T-1 provided the T-1 neck is the same thickness as the neck thickness required for the carbon steel material.

It is often necesssary to use standard flanges to match connecting valves or other fittings. Therefore, there is sometimes an economical advantage in welding a carbon steel welding neck flange to a T-1 neck as was done in the forged test cover shown in Fig. 8 (*a*), and in the built-up fitting shown in Fig. 9.

Because of necessity the Code does not require that fittings be X-rayed even though the main portion of the shell must be X-rayed to utilize the increased joint efficiency. This, too, offers a weak link in the design since the inspection requirements are relaxed for the part which, to a great extent, determines the necessity for a large factor of safety. It is certainly possible to fit and weld together the neck, the shell and the reinforcement in such a way as to provide the same reinforcing value as the inserted type nozzle. However, assurance of freedom from cracks and notches cannot be determined as easily as for the weld of an inserted forging.

The adoption of higher design stresses should rule out the attachment of nozzles by welding from one side only and by partial penetration welds; and should require more specific inspection rules, such as the use of magnetic particle or supersonic inspection where fillet or blind groove welds are tolerated.

Bolted Flanges

The flat cover, the studs and nuts, and the 16-in. diam bolting flange shown in Fig. 8 (*a*) were all T-1 steel. The bolting flange thickness and the drilling were based upon ASA 400# standard. Since the tests were of the vessels rather than the bolted cover, the flanged connection was overdesigned. The design was based upon a working stress of 50,000 psi at an internal pressure of 2500 psi. The maximum test pressure was 2850 psi; therefore, no plastic distortion occurred.

Leakage did occur, however, on the original four burst test vessels at pressures between 2200 and 2400 psi. The same gaskets, bolts and flanges were used on vessel No. 5; however, a torque wrench was used to tighten the bolts. A small leak occurred in one gasket at 2740 psi whereas no leakage occurred in the other at 2770 psi. A torque of 1400 ft-lb was applied to the bolts causing a load of 55,000 to 60,000 psi in the bolts whereas it was estimated that only 30 to 40% of this torque was applied to the original bolts.

Table 4 lists various stresses in the bolted flange connection. It is difficult to draw conclusions from such limited data, but it appears that T-1 flanges can be designed by the existing Code rules using stresses somewhat higher than one-fourth the ultimate. However, because the elastic movement may be considerably greater than experienced with present Code materials, care should be exercised in selection of gasket material and in tightening the bolts. The use of a torque wrench is recommended.

Restraints

It should be remembered that local bending stresses caused by symmetrical restraint such as heads and rings are functions of the membrane stresses in the shell; therefore, if the stress concentration is two and the shell membrane stress is 50% of the yield strength, the local stress at the concentration is at the yield strength of the material. This is true regardless of the material strength. For this reason the careful designer using higher design stresses will prefer the hemispherical or two-to-one ellipsoidal head in preference to the shallow torispherical, conical or flat heads.

On the other hand, a ring of high-strength steel such as T-1 at the region of high local bending stress can accommodate this high bending stress since its elastic movement can be two or three times that of the carbon steel. Figure 10 illustrates an expensive transition fillet insert in a penstock.[9] This solution reduced the calculated maximum combined stress to 22,800 psi. An insert of high-strength material would probably cost less and would not be overstressed since the maximum combined stress would be 30,500 psi if the shell were a uniform thickness past the stiffener.

It should also be remembered that the elastic movements of high-strength steel such as T-1 steel might overstress any restraining ring of carbon steel around the T-1 vessel. Supports and other permanent attachments such as lugs should be of the same material as that of the high-strength shell. Such attachments must necessarily move with the shell or be free to move independently. A rigid attachment of carbon steel would be stressed above its yield strength when the T-1 vessel is tested, and would work between a maximum and minimum having a magnitude approaching the yield strength each time the vessel went through its working cycle.

The following statement appeared in an analysis of ship failures:[10]

"As cracks originating in any part of a welded structure may propagate through the welds into a strength member, the standard of quality of materials, design and workmanship in all parts attached to the structure by welding should be equal to those required in the main strength members."

The experience gained by the studies of ship failures should be utilized in making safe designs of high-strength materials.

Table 4—Stresses in 16-In. Forged T-1 Manhole Flange and Cover

	Stress in psi	
Member	*Int pressure = 2740 psi (first leakage*)*	*Int pressure = 2850 psi (vessel shell fail.)*
$1^3/_8$-in. diam T-1 studs	53,000 psi	55,150 psi
3.25-in. diam T-1 cover	62,000 psi	64,500 psi
2.5-in. diam T-1 flange		
Radial flange stress, S_R	40,300 psi	41,900 psi
Tang. flange stress, S_T	22,600 psi	23,500 psi
Long. hub stress, S_H	56,400 psi	58,700 psi
$^1/_2(S_H + S_R)$	48,350 psi	50,300 psi

* Bolts tightened with torque wrench.

Welding Design

It is also important that all attachment welds that move with a T-1 shell be made with low-hydrogen high-strength electrodes and preferably those that deposit weld metal as strong as the shell of the vessel.

Cellulose-coated electrodes are apt to cause under-bead cracking. Then, too, the weaker weld metal which penetrates the shell will cause a point of weakness. Temporary attachments for fit-up devices can be structural steel provided they are removed. Such attachments and any repairs should be welded with low-hydrogen high-strength electrodes.

The results of the T-1 tests showed little to choose between the (Ni-Mo-V type) electrodes of the E12015 class in the as-welded condition as compared to the (Mn-Mo type) electrodes of the E9015 class in the stress-relieved condition. However, observations indicated that the vessels were more uniformly stressed in the case of the E12015 electrodes whereas there was some tendency to neck the welds in the vessels welded with E9015 electrodes. In two cases the failure actually took place in the main longitudinal weld made with E9015 electrode, since this weld metal was slightly weaker than the T-1 plate.

On the other hand, it is most important to know that welds made with electrodes containing appreciable quantities of vanadium, as was the case with the E12015 electrodes used, *cannot* be stress relieved because such treatment embrittles the weld. The ductility transition temperature by the Charpy keyhole notch was raised from −60° F as-welded to above 130° F stress relieved.

Automatic welding of T-1 steel has not yet been fully developed. However, there is reason to believe that multipass welding will eventually be possible. Tests of semi-automatic welds made by the inert-gas-shielded metal arc process have indicated satisfactory results can be obtained. As in the case of manual welding, automatic welds made with electrodes containing an appreciable amount of vanadium *cannot* be stress relieved.

Electrodes of the E11015 class are also being tested. Indications are that the resulting welds will be as strong as the parent plate and that these welds can be stress relieved without raising the transition temperature appreciably.

Use with Other Materials

It has already been pointed out that carbon steel flanges or necks can be welded to T-1 necks designed at stresses permitted in the carbon steel provided the seam is beyond the influence of the reinforcement area.

Plate sections or complete rings of T-1 plates have been used in vessels to prevent or slow down rapid abrasion. T-1 plates are also used where large elastic movements with high strength are desirable as in flex plates of wind tunnel nozzles.

It is possible that a T-1 vessel with a thin stainless welded strip liner might not stand up under the release of the overload test pressure and subsequent working loadings. The test pressure would cause stresses approaching 50,000 psi in the stainless steel. On the other hand, the test of an integrally clad vessel would leave the liner in residual compression. Tests on this problem are under way.

Economies

Until the Code authorities establish allowable design stresses cost comparisons mean little. Some idea of the factors involved are summarized in Table 5. These figures are quite approximate and many factors, such as stress-relief limitations on thickness, could upset these completely. Then, too, the experience to date is limited; new electrodes and methods will undoubtedly follow.

The material costs are realistic, but even here a requirement such as normalizing and guaranteed Charpy values for the A-201 would reduce the T-1 factor to 2A instead of 2.5A.

The fitting cost factor would increase unfavorably for the T-1 if the savings in weight of the bolting flange cannot be realized of if more expensive construction is required.

Table 5—Approximate Comparable Cost Factors

Identical field erected vessels	*A-201*	*T-1**
Plate material costs	A	2.5A
Fittings costs	B	2B
Fabrication costs	C	1.3C
Erection costs	D	1.1D
Welding (hand) costs	E	1.5E
X-ray costs	F	F

Identical shop built vessels	*A-212*	*T-1**
Plate material costs	A	2.5A
Fittings costs	B	2B
Fabrication costs	C	1.3C
Shop assembly costs	D	1.1D
Welding costs		
Automatic	E	...
Hand	..	3E
X-ray	F	F
Stress relief	G	G

* In order to make an actual cost comparison these factors must be multiplied by the ratio:

$$\frac{\text{A-212 or A-201 allowable stress}}{\text{T-1 allowable stress}}$$

The fabrication cost factor depends upon the capacity of the machines involved. The cost factor cannot be evaluated if machines available are not adequate to form the T-1. Because of its higher yield strength considerably more power is required to form T-1 plate than is required to form the same thickness plate either of A-201 or A-212. Hot forming of T-1 material requires quenching and tempering after forming. In general it is advisable to purchase quenched and tempered material even though subsequent hot work is to be done.

The shop assembly cost factor is slightly higher for the T-1 steel because of the additional care required and the necessity of welding all attachments with low-hydrogen high-strength electrodes.

It is really impractical to compare the welding cost in a general figure, so the values given cannot mean much. This cost depends upon the procedure used, the thickness involved, and many other factors. It is well to remember that for cases where low-hydrogen electrodes must be used for A-201 and A-212 the additional welding cost for T-1 is reduced considerably.

The difference in X-ray and stress relief, if required for both, cannot make much difference for the same thickness of metal. However, it will make a difference if the requirement is mandatory for one and not the other. It is important to note that a 10% weight saving more nearly offsets the cost of X-ray for the more expensive T-1 steel than for the less expensive steels. Also that stress relief has practically no effect on the notch toughness of T-1 steel. Stress relief of T-1 must be done below the tempering temperature (1150 to 1200° F) of the plate.

From this comparison it is apparent that the allowable stress for the T-1 steel must be considerably greater than that of either A-201 or A-212 before the use of T-1 steel can be justified. The exceptions to this are the cases where stress-relief requirements, limiting plate thickness, low temperature service or other considerations permit the use of T-1 but prohibit the use of the other materials without adding substantially to their costs.

Conclusion

The design of pressure vessels using high-strength quenched and tempered steel is practical, but special design considerations must be given because this is a new material and as such requires new thinking. A design stress level higher than that given by the present Code rules seems justified for this new material. Refinements of design details and increased inspection requirements would be required to permit design stresses greater than one-fourth of the ultimate strength. It is realized, however, that Code stress levels based upon yield strength or a greater portion of the ultimate strength require careful consideration as to the effect of any new standards upon all Code materials.

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SUITABILITY OF QUENCHED AND TEMPERED STEELS FOR PRESSURE—VESSEL CONSTRUCTION

Amount of ductility needed for fabrication and operation, elastic ratios, relationship between ductility and toughness, the necessity for stress relieving, are among the subjects discussed in addition to describing destructive tests of 8 full-scale pressure vessels

BY LEON C. BIBBER

Introduction

The suitability of quenched and tempered steel, or any other steel, for the construction of pressure vessels is in most cases determined by the many regulatory bodies which control the design and construction of such vessels. The best known of these agencies is the Boiler and Pressure Vessel Committee of the American Society of Mechanical Engineers.

In passing judgment on the suitability of any steel for pressure vessel construction, the various regulatory bodies must weigh the advantages and disadvantages of a new steel very carefully, particularly if the new material is a radical departure from those commonly used. It is hoped that the discussion in this paper and the comment that will be evoked will be of help to those who must make such decisions.

Chemical Composition

While the title of the paper encompasses quenched and tempered steels in general, the discussion is largely confined to one such steel; namely, "T-1"* steel. This is a proprietary steel, but at least one other manufacturer has been licensed to produce this material.

The chemical composition range and the minimum mechanical properties for "T-1" steel, 1/4 to 2 in. thick, are given in Reference 1, at the end of this paper.

Also, since this paper will be concerned largely with the performance of plates made from a single heat of this steel, the ladle analysis of this particular heat is given below:

Heat No. 74L236—Ladle Analysis, %	
Carbon	0.15
Manganese	0.92
Phosphorus	0.014
Sulfur	0.020
Silicon	0.26
Nickel	0.88
Chromium	0.50
Molybdenum	0.46
Vanadium	0.06
Copper	0.32
Boron	0.0031

Ultimate Tensile Strength

In Table 1 are shown the mechanical properties of many 1/2-in.-thick plates made from this heat. These plates were quenched cold from 1650° F and then were tempered at 1175° F and again quenched cold.

This steel is manufactured to a minimum tensile strength of 105,000 psi, but the actual tensile strengths of this particular group of plates were considerably in excess of the minimum and were practically double those of the structural carbon steels so often used for pressure vessel construction. It will also be noted that the strengths obtained from transverse specimens were essentially equal to those obtained from longitudinal specimens.

When this steel is ordered for pressure vessel construction, it is suggested that fire box quality be specified. The fact that the plates are to be used for the construction of pressure vessels should also be stated on the order. The plates will then be cross-rolled as shown in Fig. 1. It will be seen that the slab (one of many obtained from the ingot) is rolled to the desired width and then turned 90 degrees and the final rolling done with the axis of the ingot perpendicular to the direction of rolling. Cross-rolling in this manner minimizes directional properties and makes the plates particularly suitable for a biaxial stressing such as obtained in spheres.

On "T-1" steel for the fabricating of

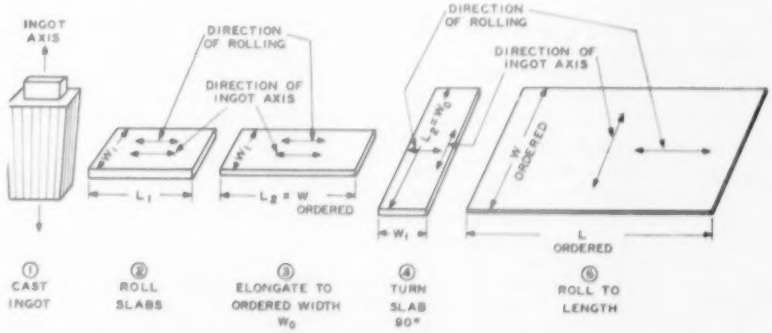

Fig. 1 Cross rolling of "T-1" steel for pressure vessel construction

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* "T-1" is a trademark of the United States Steel Corporation.

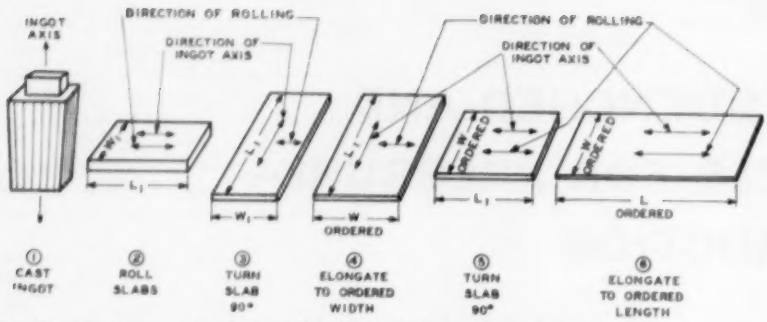


Fig. 2 Straightaway rolling of "T-1" steel for structural purposes

structures other than pressure vessels, a certain amount of cross-rolling may be done but that amount will be incidental to obtaining the ordered plate width from the slab width. In Fig. 2 is shown the straightaway rolling of plate. It will be seen that the slab (one of many) is turned 90 degrees and cross-rolled until the desired width is obtained and then the slab is again turned and rolled with the ingot axis parallel to the direction of rolling.

One plate of heat-treating size is rolled from each slab. Each plate as-heat-treated is subject to extensive testing for routine control. The number and type of specimens are shown in Fig. 3. Longitudinal tension specimens are taken from the top and bottom of each plate, as-heat-treated. If the plate as-heat-treated were 40 ft long and were to be cut into two plates 20 ft long, the specimens would be cut from the 40-ft plate as shown, but not from each of the 20-ft plates cut therefrom.

Yield Strength

This steel is manufactured to a minimum yield strength of 90,000 psi. In Table 1, it will be noted that the yield strengths of the various plates are well above this minimum. Again, the similarity between longitudinal and transverse values and the similarity between the results of tests from the different parts of the plate will be noted. Yield strength is used with this material because this steel, like practically all alloy

Table 1—Mechanical Properties of Quenched and Tempered 1/2-In.-Thick Plates for Construction of Eight Pressure Vessels. Firebox Quality

Slab No.	*Thickness, in.*	*Orientation and location of specimen*	*Yield strength, 0.2% psi*	*Ultimate strength, psi*	*Elastic ratio, %*	*Elong. in 8 in., %*	*Elong. in 2 in., %*	*Charpy keyhole* — *Impact value at −50° F, ft-lb*	*Charpy keyhole* — *Ductility-transition temperature, ° F*	*Hardness (Vickers)*
62670	0.523	Long. bot.	119,700	123,300	97.0	..	27	34-33-32	−217	272
	0.515	Long. top	124,400	127,800	97.4	..	28			
	0.512	Trans. top	124,400	129,200	96.3	..	27	23-22-22		
62672	0.516	Long. bot.	107,300	113,150	94.9	..	32.0	33-31-33	−197	260
	0.513	Long. top	106,700	115,650	92.2	..	29.0			
	0.510	Trans. top	111,000	117,400	94.4	..	31.0	29-28-27		
62674	0.516	Long. bot.	115,600	121,600	95.1	10.7	29.0	32-32-32	−217	272
	0.513	Long. top	111,000	117,100	94.5	11.5	31.0			
	0.517	Trans. top	112,200	119,200	94.1	10.7	27.0	24-25-24		
62675	0.516	Long. bot.	121,400	128,300	94.5	11.0	31.0	31-31-28	−208	294
	0.526	Long. top	123,100	128,800	95.8	11.7	26.0			
	0.527	Trans. top	123,800	130,700	94.6	8.5	26.0	22-23-21		
62676	0.514	Long. bot.	116,300	123,300	94.3	10.5	28.0	33-35-34	−225	284
	0.515	Long. top	110,500	117,500	94.1	10.2	28.0			
	0.511	Trans. top	112,300	121,400	92.5	11.0	23.0	26-25-26		
62677	0.523	Long. bot.	121,400	127,000	95.6	10.2	28.0	32-32-30	−200	283
	0.520	Long. top	114,600	121,400	94.4	11.0	31.0			
	0.516	Trans. top	117,100	123,700	94.9	8.0	22.0	25-25-25		
62678	0.511	Long. bot.	114,500	122,400	93.5	13.0	27.0	30-30-30	−205	277
	0.509	Long. top	111,600	120,900	92.3	10.7	25.0			
	0.511	Trans. top	113,900	123,600	92.1	11.5	29.0	27-28-27		
62679	0.513	Long. bot.	124,800	133,600	93.4	10.5	23.0	29-31-33	−235	298
	0.509	Long. top	115,100	131,200	87.7	9.7	28.0			
	0.515	Trans. top	125,500	135,300	92.7	10.5	28.0	25-24-25		
62680	0.520	Long. bot.	114,100	123,100	92.7	10.7	29.0	36-34-34	−205	275
	0.515	Long. top	107,700	115,100	93.3	15.2	32.0			
	0.522	Trans. top	109,600	117,200	93.4	10.5	27.0	27-28-28		
62681	0.504	Long. bot.	116,100	128,600	90.5	12.0	27.0	31-30-31	−238	286
	0.512	Long. top	119,900	126,700	94.5	11.7	22.0			
	0.507	Trans. top	120,600	129,200	93.2	10.7	26.0	23-23-22		
62682	0.524	Long. bot.	125,300	132,900	94.4	11.5	28.0	29-27-30	−208	296
	0.523	Long. top	120,900	127,900	94.5	13.2	31.0			
	0.525	Trans. top	119,100	131,000	91.0	10.0	27.0	22-22-22		
62683	0.497	Long. bot.	126,700	132,200	95.5	10.2	27.0	34-30-31	−235	292
	0.495	Long. top	121,050	128,200	94.4	13.0	25.5			
	0.495	Trans. top	125,100	132,000	94.8	9.0	25.0	22-22-21		
62687	0.517	Long. bot.	125,500	133,300	94.1	9.0	27.0	31-30-30	−230	307
	0.517	Long. top	120,700	127,300	94.6	10.5	28.0			
	0.512	Trans. top	116,800	131,100	88.9	9.2	24.0	22-20-21		
62688	0.517	Long. bot.	108,500	117,000	92.7	..	29.0	29-32-29	−194	255
	0.503	Long. top	112,700	121,600	92.6	..	28.0			
	0.505	Trans. top	118,000	124,550	94.8	..	27.0	24-23-22		

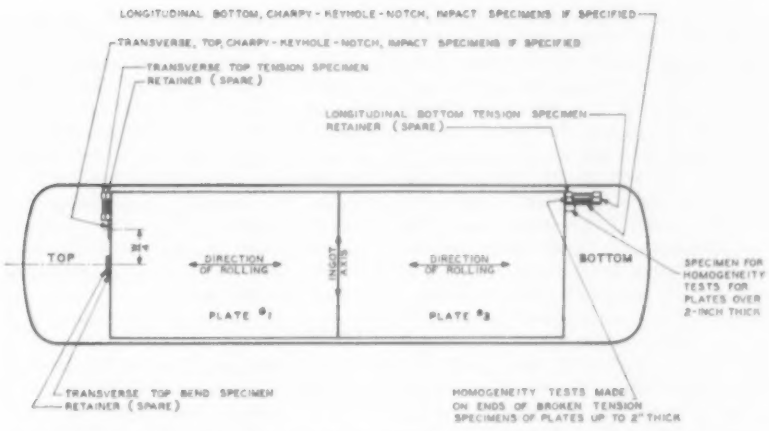

LONGITUDINAL BOTTOM, CHARPY - KEYHOLE - NOTCH, IMPACT SPECIMENS IF SPECIFIED
TRANSVERSE, TOP, CHARPY - KEYHOLE - NOTCH, IMPACT SPECIMENS IF SPECIFIED
TRANSVERSE TOP TENSION SPECIMEN RETAINER (SPARE)
LONGITUDINAL BOTTOM TENSION SPECIMEN RETAINER (SPARE)
TOP
DIRECTION OF ROLLING
INGOT AXIS
DIRECTION OF ROLLING
BOTTOM
PLATE #1
PLATE #2
SPECIMEN FOR HOMOGENEITY TESTS FOR PLATES OVER 2-INCH THICK
TRANSVERSE TOP BEND SPECIMEN RETAINER (SPARE)
HOMOGENEITY TESTS MADE ON ENDS OF BROKEN TENSION SPECIMENS OF PLATES UP TO 2" THICK

Fig. 3 Tests taken on plate (as heat treated) of "T-1" steel of firebox quality

steels, does not have a definite drop-of-the-beam yield point. The stress-strain curve, however, is "sharp-kneed" as will be shown later.

Possibly there are some who are laboring under the belief that the attainment of such high yield strength is difficult. That this is not the case can best be shown by the results of some tests that were conducted on plate which was quenched only. The microstructure of the 1/2-in. plate throughout the thickness was untempered martensite. Tension specimens of this material had the following properties:

Ultimate tensile strength	194,000 psi
Yield strength	139,600 psi
Elongation in 2 in.	14.0%
Reduction area	52.5%

This then is in reality the basic material which is to be tempered to suit requirements. Using higher tempering temperatures, lower yield strengths can be obtained; or by using lower tempering temperatures, yield strengths even approaching the value given above can be achieved. This condition is quite different from that existing in hot-rolled steel, in which the resulting mechanical properties are principally a function of composition, finishing temperature and rate of cooling which generally is dependent on thickness. In this quenched and tempered steel, mechanical properties vary but little with thickness in the range say up to 2 in. thick which would be used for pressure-vessel construction. This is shown in Table 2.

In Table 1, under the column headed Elastic Ratio, are given the ratios of the yield strengths to the ultimate strengths. It will be seen that these ratios are extremely high, so much so as to be alarming to some. There is considerable volume of engineering opinion which believes that there should be a wide margin between the yield strength and the ultimate; in fact, there are structural specifications extant which prohibit the use of yield strength in excess of 70% of the ultimate. Such restrictions are the result of the belief that a low elastic ratio is assurance of toughness. As will be shown later with quenched and tempered "T-1" steel, toughness can be accompanied by very high elastic ratios. In fact, yield-ultimate ratios in excess of 80% are a good *indication* that proper heat treating has been carried out.

If further confirmation of toughness is required, notch-bar impact testing of selected areas can be done, but it must be remembered that if a plate is large enough to require the whole product of a slab, no destructive-test specimens can be taken at the centroid of the area without ruining the plate.

One problem which has troubled regulatory bodies in deciding upon the suitability of quenched and tempered plates for pressure vessel construction has been that of assurance of proper heat treatment of each plate.

Destructive tests provide information on the heat treatment of the particular area tested but give no assurance of the uniformity of heat treatment over the entire area of the plate. The uniformity of heat treatment over the total plate area depends upon the knowledge, the skill and the experience of the manufacturer. The manufacture of quenched and tempered plate for structural purposes is not new, it is only new to civilian engineers. During the past few decades, thousands of tons of quenched and tempered plate have been made for special purposes and no lack of uniformity has been found. In other words, the assurance of uniformity of heat treatment is the soundness of the practices and controls of an experienced manufacturer.

Even though yield strengths as high as 140,000 psi can be obtained in the as-quenched condition, to produce plates having the best combination of strength, ductility and toughness, it is desirable to temper the quenched plates to a somewhat lower strength level. In this quenched and tempered condition a conservative minimum yield strength of 90,000 psi can be readily attained.

Table 2—Effect of Thickness on Mechanical Properties of "T-1" Steel

Heat No. 348267

C	*Mn*	*P*	*S*	*Si*	*Cu*	*Ni*	*Cr*	*Mo*	*V*	*B*
0.15	0.70	0.016	0.021	0.20	0.29	0.88	0.59	0.50	0.09	0.0038

Quenched from 1650° F—Tempered at 1200° F

Thickness, in.	*Yield (0.2%) strength, psi*	*Ultimate strength, psi*	*% elong. in 2 in.*	*% red. area*
3/4	111,300	122,250	21.0	65.8
1*	120,300	126,050	21.5	69.3
1*	122,800	128,800	21.0	68.1
1*	128,050	133,050	20.0	67.0
1 1/2	124,800	131,300	20.0	65.9
1 3/4*	123,050	129,800	20.5	65.9
1 3/4*	121,050	128,050	20.0	68.1
1 3/4*	123,550	130,100	21.0	70.2
2*	117,800	125,600	21.5	69.1
2*	125,800	131,800	20.0	66.9
2*	119,150	128,200	20.5	68.0

* Different slabs.

Ductility

The particular quenched and tempered steel which is under discussion is manufactured to a minimum elongation of 18% in 2 in. In tension testing alloy steel, the 2-in. gage length is customarily used, but since structural carbon steels are sold to 8-in. gage-length requirements, the values for elongations in 2 in. and 8 in. are both shown in Table 1. It will be noted therein that the average elongation in 2 in. is about 26%, and the average elongation in 8 in. is about 11% with individual values as low as 8.5%. The material in question was cross-rolled to produce approximately the same properties in both directions, and it will be seen that relatively good equality of elongation was obtained in most cases.

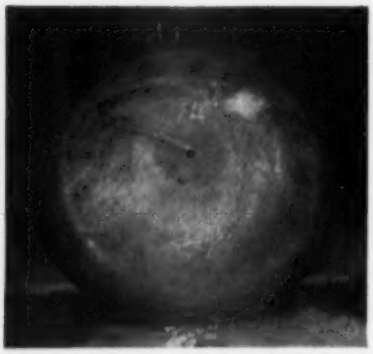


Fig. 4 Flanged and dished head

Plate thickness	3/4 in.
Inside diam	48 in.
Radius of dish	48 in.
Radius of knuckle	3 1/2 in.
Length of flange	1 1/2 in.

Inasmuch as the elongations in 8 in. shown are considerably less than are found in pressure-vessel carbon steels, the question immediately arises as to how much elongation is necessary for the construction of pressure vessels. There are really two parts to this question: (1) How much ductility, as measured by elongation in the tension test, is necessary for the fabrication of pressure vessels? (2) How much ductility so measured is necessary for the operation of pressure vessels?

Let us consider the first part of this question. A certain amount of rolling, flanging, dishing and other operations must be done in the fabrication of pressure vessels. The only answer to the question as to whether or not these operations can be satisfactorily carried out is to actually do them. In Fig. 4 is shown a flanged and dished head fabricated by the Chicago Bridge & Iron Co. This head was made of 3/4-in-thick material and was progressively flanged and dished *cold*. One head was even fabricated cold with a welded joint in the middle. It is believed that the forming shown in Fig. 4 is about as difficult an operation as will be found in pressure vessel construction, and the fact that this severe cold forming was successfully carried out is a demonstration that the amount of elongation shown in Table 1 is adequate for pressure vessel fabrication. It should be borne in mind, however, that due to the great strength of this steel, much more force would be required for these operations than is required for pressure-vessel carbon steels of the same thickness.

This brings us to the second part of the question; namely, how much ductility, as measured by elongation in the tension test, is necessary for the operation of pressure vessels? Theoretically, if a vessel containing no stress raisers is capable of holding pressures which develop membrane stresses up to the yield strength of the material, such a vessel could be made of glass and no ductility whatsoever would be needed. If this theoretically perfect vessel were made of steel having a large amount of elongation measured in a streamlined tension specimen, that perfect vessel would, as the membrane stress was raised above the yield strength, balloon to the limit of the extensibility of the steel. On the other hand, if a practical vessel containing nozzles, fillet welds, nicks, undercuts, and other stress raisers were built of this same steel and tested at a sufficiently low temperature, the vessel would fail in a brittle manner with little or no ballooning despite the fact that the material of which the vessel was constructed had large amounts of elongation in a streamlined specimen. If the hypothetical situation as described above be true, does it not follow that toughness (even though accompanied by lesser amounts of streamline elongation) under the adverse conditions of low temperature and notching is more important than large amounts of elongation in a specimen which does not in any way simulate the condition existing in the actual vessel?

It so happens that most steels which are used for construction have a large amount of ductility. This is very well shown in Fig. 5, in which the stress-strain curve of pressure-vessel carbon steel is superimposed on the stress-strain curve of a quenched and tempered alloy steel. It will be seen that the elongation of the carbon steel is nearly three times that of the stronger alloy steel. It is also interesting to note, however, that the energies absorbed by the two specimens are not too much different. The great elongation of the carbon steel indicates that if no concentration of stress or other factor prevented, a vessel built of such a steel would balloon to a great degree; whereas a vessel built of the stronger and less ductile alloy steel would balloon to a much less degree. That brings up another question: is great capacity for ballooning necessary? The answer will be provided by the full-scale tests to be described later.

Toughness

Toughness, as the term will be used hereinafter, means ability to resist brittle fracture; that is, the ability to absorb work under adverse conditions of temperature, strain rate and geometry. Toughness may be measured by many of the various tests now in vogue such as the Charpy impact test, the Kinzel notch-bend test, and Naval Research Laboratory drop-weight test, the explosion bulge test, and others. The results are commonly expressed in terms of transition temperature; that is, the temperature at which a change from tough to brittle behavior takes place. Generally, two transitions occur—one is called the ductility transition, and the other the fracture transition.

The ductility-transition temperature is that temperature below which fracture can readily start in the presence of a notch. Above this temperature, crack initiation must be forced and hence is unlikely. In other words, below the ductility-transition temperature the last vestige of toughness has de-

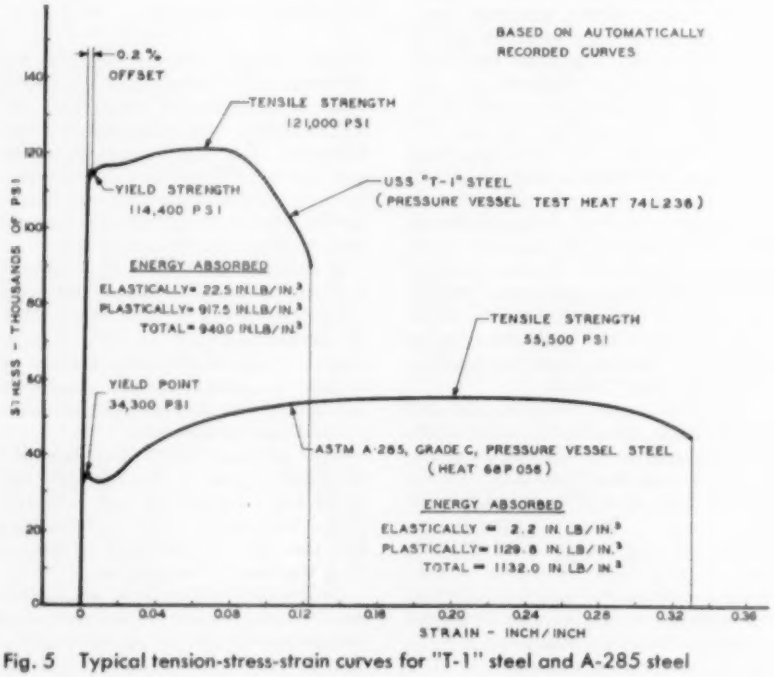


Fig. 5 Typical tension-stress-strain curves for "T-1" steel and A-285 steel

parted and the danger of crack initiation is great.

The fracture transition could more properly be named the "fracture-appearance transition," since fracture appearance is usually the criterion used. The fracture-transition temperature is frequently defined as that temperature at which 50% of the fracture has a fibrous appearance. The fracture-appearance transition is believed to be associated with crack propagation. Below the fracture-appearance-transition temperature, cracks will propagate with very little additional energy absorption; above that temperature, the material will tear in the shear mode and resist the propagation of the crack. The fracture-appearance-transition temperature is for many steels 50 to 100 degrees or more higher than the ductility-transition temperature. This means that if a crack is started, it will continue to run even at a much higher temperature than that at which it started. However, if a crack cannot start, it cannot propagate. Because of this fact, the ductility-transition temperature is considered to be the more important of the two in most applications.

Toughness and ductility are related because toughness cannot exist without some degree of ductility, but ductility (elongation) can be manifested under favorable circumstances in a material which will behave in a brittle manner in the presence of a notch. Toughness is two-dimensional, in that it involves both ductility and strength. Ductility is uni-dimensional. The requisite, therefore, for toughness is a small amount, even a minute amount, of ductility at the root of a notch and not necessarily a large amount of ductility in a streamlined tension specimen. If this small amount exists when and where it is needed, the toughness it will provide is a much better assurance against catastrophic failure than elongation which would be great in a streamlined specimen but would be absent or nonexistent when conditions become adverse.

In Table 1 are given the ductility-transition temperatures of the various $^1/_2$-in.-thick plates as evaluated by keyhole Charpy specimens. It will be seen that they are all in the vicinity of −200° F. These are the values for unfabricated plates; as is shown in Reference 1, welding, forming and other fabrication operations bring about higher transition temperatures.

The values shown in Table 1 should not be regarded as operating temperatures. Much more conservative operating temperatures should be contemplated when designing pressure vessels of "T-1" steel, because practically all fabrication operations raise the ductility transition temperature.[1]

Stress Relieving

In the early days of welding, some rather puzzling failures occurred to welded pressure vessels subjected to no pressure whatsoever. Only residual stress could have been involved. The practice of stress relieving was then instigated and the failures stopped. These occurrences were apparently proof that residual stress caused some failures and that stress relieving prevented such failures. As a result, the practice of stress relieving is well established and is required by the Code in many cases.

Since the epidemic of brittle failures in ships during the last war, a great deal of study has been directed to the flow and fracture of metals. A number of investigators[2,3] have shown that the thermal treatment called stress relieving

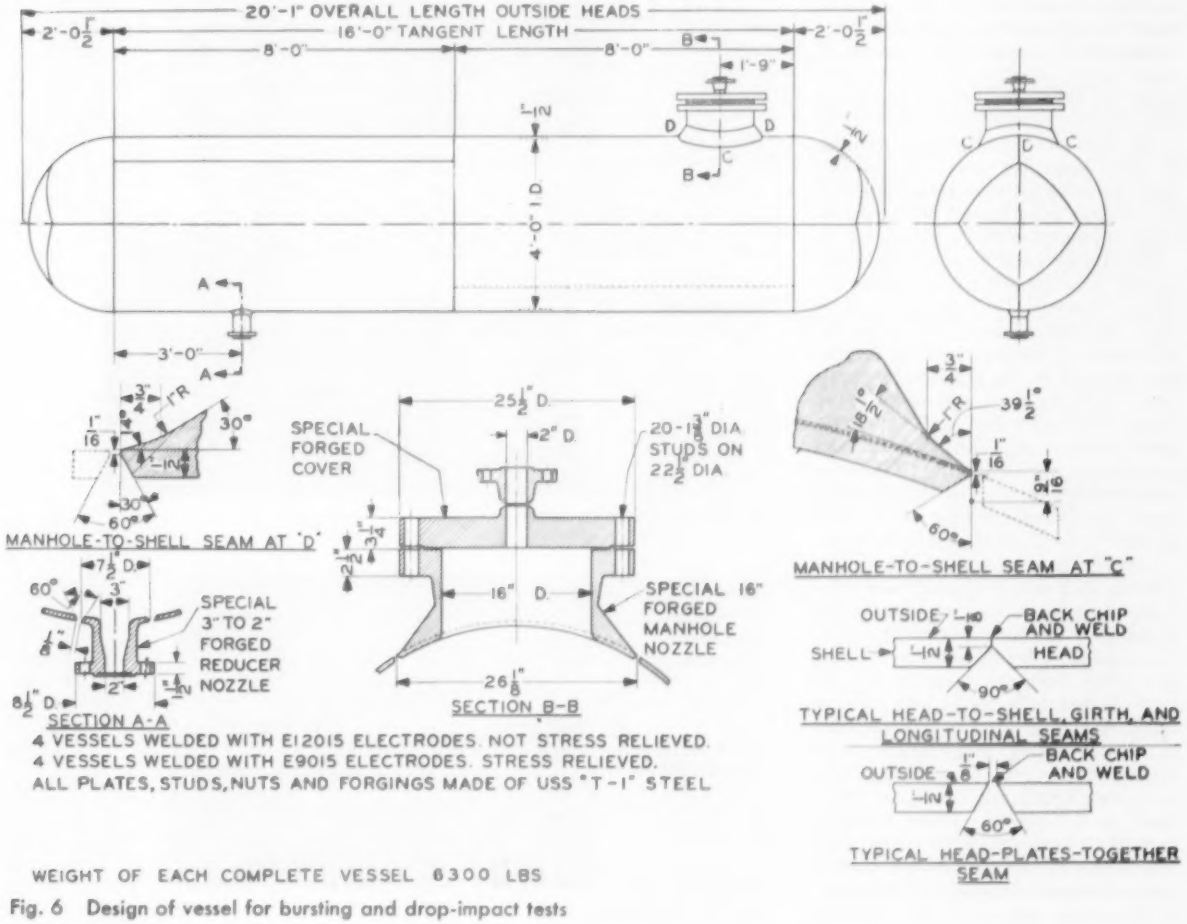


Fig. 6 Design of vessel for bursting and drop-impact tests

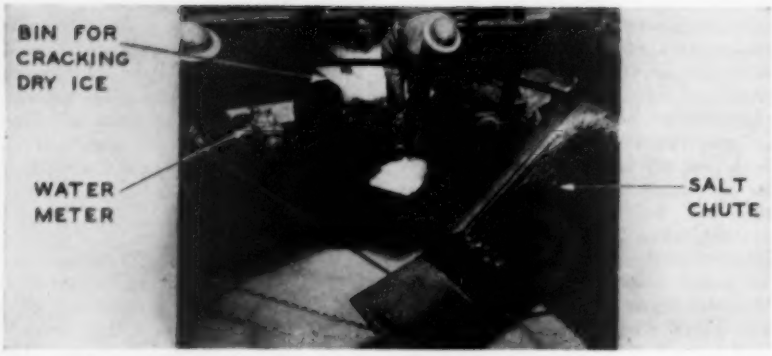


Fig. 7 General view of cooling tank—capacity 2350 gal.

in addition to lowering the residual peak stresses in welded parts often restores, or nearly restores, the original base-metal toughness. This gives rise to the question, if the welded steel were tough at the operating temperature, would it make any difference whether residual stresses were present or not? If it could be proved that their presence made no difference, then vessels made of welded steel which was tough at the lowest operating temperature need not be stress relieved.

On the other hand, if the steel after having been welded were not tough, it might not be able to make the minute adjustments necessary to distribute peak welding stresses and failure could occur in a brittle manner at a relatively low average stress.[6] In such cases stress relief would be beneficial because it could lower the peak stresses and hence permit a greater average stress to be endured even though no improvement in toughness had occurred.

The Boiler Code requires stress relieving in welded pressure vessels for three reasons: (1) thickness greater than specified minima; (2) degree of cold work; (3) large amounts of hardening elements in the chemical composition. All three of these items affect the toughness of hot-rolled steels adversely, and if there is any likelihood that the operating temperature will be below the transition temperature of the *fabricated vessel*, then stress relieving as now prescribed by the Code should be done.

Certain thermal operations before welding such as normalizing,[3] and more particularly quenching and tempering, so improve the toughness of the plate that even after deterioration by welding and other fabrication operations the heat-affected base metal may still be tough enough to function satisfactorily at the operating temperature. This is particularly true of quenching and tempering which can result, in "T-1" steel, in a lowering of the ductility-transition temperature by more than *200 degrees* (1) by the Charpy keyhole test. Chemical composition, therefore, is not a true criterion of the necessity for "stress relieving"; the microstructure of the plate is a better guide.

There is an interesting difference between the quenched and tempered "T-1" steel and pressure-vessel carbon steels. The toughness of the latter after having been impaired by cold working is benefited to but a small degree by subsequent stress relieving.[7] On the other hand, the toughness of cold-worked "T-1" steel is fully restored[1] by this treatment.

Another factor involved in the performance of pressure vessels is stress concentration. The peak stresses resulting from sharp changes in direction and from discontinuities are a function of the geometry of the structure. Stress relieving does not alter that geometry in any way, but can lower the residual peak stress in the vicinity of the stress raiser and can improve the toughness of the material. If the toughness had been seriously impaired locally by welding, a concentration of stress could be dangerous because the material in that vicinity, whether weld metal or heat-affected base metal, might not permit the plastic flow necessary to allow the part to distribute the stress. On the other hand, if the material is tough at the temperature involved and

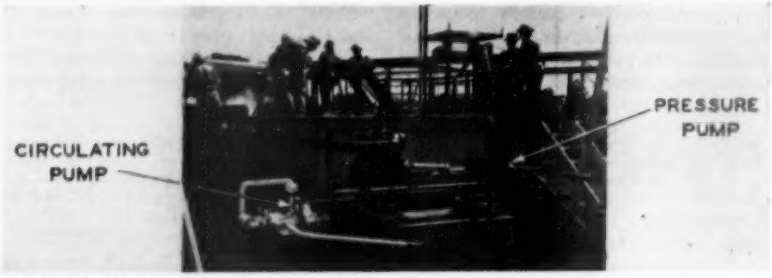


Fig. 8 Circulating and pressure pumps

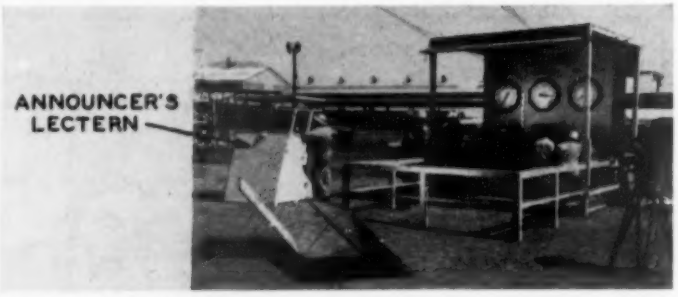


Fig. 9 Instrument console

Fig. 10 Burst-test stand

under the state of stress existing in the as-welded condition, a concentration of stress is no more harmful unstress-relieved than it is stress-relieved, because a minute amount of plastic flow will bring about a readjustment which will radically change the stress concentration factor.

There is one instance in which the relief of stress is beneficial in fabrication even though the vessel is tough at the time. If precise machining must be done, a stress relief may be necessary to ensure that unbalanced machining disturbs the equilibrium of the internal forces as little as possible.

When stress corrosion is a factor in the operation of a vessel, residual stresses may require consideration even though the material in the vessel is tough. In structural carbon steels, the measured residual weld stress is far in excess of the yield strength of the base metal.[3] In the case of quenched and tempered plate, the residual weld stress measured in $^1/_2$-in.-thick plate was in the vicinity of the yield strength of the base metal and of the weld metal, that is, about 80,000 psi.[1] In pressure vessels built of other types of steel practically every failure attributed to stress corrosion occurred when the stress was at about the yield strength of the material. It would appear, therefore, that if stress corrosion were a danger, the vessel should be given a stress-relief treatment for the purpose of reducing the stress peaks as much as possible.

It will be seen that the situation with regard to stress relieving pressure vessels built of quenched and tempered "T-1" steel is somewhat different from that which obtains in the case of vessels built of pressure-vessel carbon steels. This difference lies in the fact that the "T-1" steel—after having its toughness impaired by welding, cold work and other fabrication operations—is still very tough[1] and, as will be shown later, does not need to have its toughness restored for operation at even the lowest atmospheric temperatures. If actual reduction of peak stresses is desired rather than restoration of toughness, then stress relieving should be done. However, in considering the necessity for stress relieving, the importance of the toughness restoration aspect of this process must always be borne in mind.

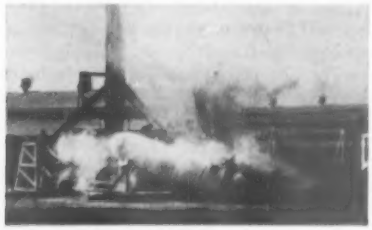


Fig. 11 Typical burst

Fig. 12 Typical burst failure

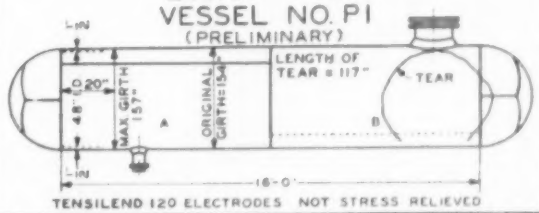
Tests of Full-Scale Vessels

Even though the ideas advanced in the foregoing are well supported by laboratory testing, it was realized that their validity must be demonstrated by tests of full-scale vessels. Accordingly, the Chicago Bridge & Iron Co. and the United States Steel Corp. decided to test a number of such vessels, to demonstrate:

1. The fitness of quenched and tempered plate for the construction of pressure vessels.
2. The propriety of utilizing the great strength potential of this type of steel; that is, the propriety of using much higher working stresses than have been permitted heretofore.
3. The desirability of eliminating the present requirements for stress relieving when restoration of toughness is unnecessary.

This test, called "Operation T-1," was conducted at the Birmingham plant of the Chicago Bridge & Iron Co. on June 29 and 30, 1954. At that time, four vessels were tested; two in the as-welded condition, and two stress relieved. Four vessels had been tested in preliminary trials so that "Operation T-1" destroyed a total of eight vessels.

BURST TEST
VESSEL NO. P1
(PRELIMINARY)



TENSILEND 120 ELECTRODES NOT STRESS RELIEVED

PL NO	SLAB NO	YIELD STR 2% PSI ①	ULT STR PSI ①	ELAS RATIO % ①	ELONG IN 2" % ①	ELONG IN 8" % ①	RED OF AREA % ①	CHARPY KEYHOLE ② ENERGY AT -50°F FT LB	CHARPY KEYHOLE ② DUCT TRANS TEMP	HARD VICK
A	62676	113000	120700	93.6	26.3	10.6	51.0	33-35-34	-225	264
B	62676	113000	120700	93.6	26.3	10.6	51.0	33-35-34	-225	264

AMBIENT TEMP AT TIME OF TEST	+58 °F
TEMP OF VESSEL AT START OF TEST	-45 °F
TEMP OF VESSEL AT FAILURE	-41 °F
BURSTING PRESSURE	2740 PSI
INCREASE IN VOLUME 41.9 GAL	2.38 %
CHARACTER OF FRACTURE 100% 45 DEG SHEAR	
TIME REQUIRED FOR BURSTING	113 MIN
NOMINAL STRESS (48" DIAM & 1/2" THICK)	131,800 PSI
MAXIMUM OUTSIDE DIAM 50.3 IN INCREASE	2.66 %
MAXIMUM STRESS (MAX INSIDE DIAM & CALC MIN THK)	135,000 PSI
MAXIMUM INCREASE IN GIRTH	3 IN

① AVG OF LONG & TRANS VALUES
② DUCTILITY TRANS TEMPS AT 12-14 FT-LB

Fig. 13 Burst test data for vessel No. P1

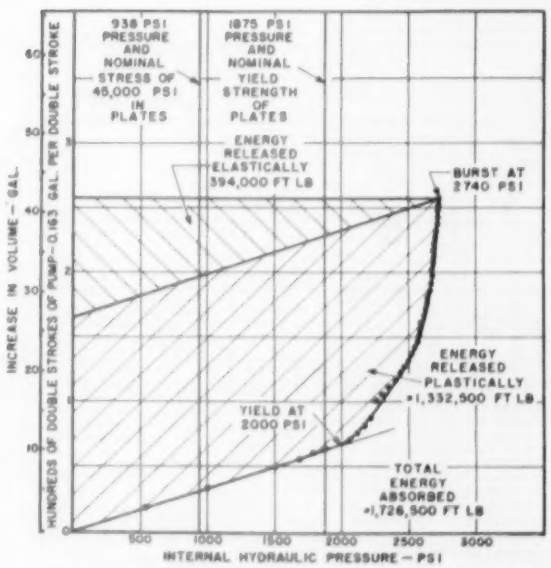

938 PSI PRESSURE AND NOMINAL STRESS OF 46,000 PSI IN PLATES
1875 PSI PRESSURE AND NOMINAL YIELD STRENGTH OF PLATES
ENERGY RELEASED ELASTICALLY 394,000 FT LB
BURST AT 2740 PSI
ENERGY RELEASED PLASTICALLY 1,332,500 FT LB
YIELD AT 2000 PSI
TOTAL ENERGY ABSORBED 1,726,500 FT LB
INCREASE IN VOLUME — GAL.
HUNDREDS OF DOUBLE STROKES OF PUMP — 0.163 GAL. PER DOUBLE STROKE
INTERNAL HYDRAULIC PRESSURE — PSI

Fig. 14 Pressure-volume curve for burst test of vessel No. P1

The design of the vessels is shown in Fig. 6. It will be seen that hemispherical heads were used. The thickness of these heads could have been reduced to about half of that shown, but to simplify material procurement it was decided to make the head thickness the same as that of the shell. It will be seen that inserted nozzles were set into the shell. It would have been simpler to have placed the nozzles in the heads, but it was considered desirable to locate them as shown because their presence in the shell would create more harmful concentrations of stress. The design of the nozzles was such, however, that this concentration would be reduced to a minimum.

It will be noted in Fig. 6 that the vessels which were to be tested as fabricated, were welded with an E12015 electrode. This rod deposits Ni-Mo-V weld metal which has excellent properties as-welded, but is drastically embrittled by post heat treatment.[1] Hence, the vessels which were to be stress relieved were welded with an E9015 electrode. This rod deposits Mn-Mo type weld metal of somewhat less than base-metal strength, but of adequate toughness after having been stress relieved.

Since toughness is a most important consideration, it was decided to test the vessels at temperatures as low as the ductility-transition temperature obtained by Kinzel tests of welded specimens. This temperature was found to range from −42 to −50° F in the as-welded condition and, accordingly, −50° F was set as a target. This temperature was also desirable because it could be obtained through the use of calcium-chloride brine and dry ice, relatively cheap refrigerants. The specific gravity of the brine had to be maintained very accurately at 1.29%. For each test more than ten thousand pounds of calcium-chloride salt were needed to obtain this salinity in the liquid in the system, and more than eleven thousand pounds of dry ice were necessary to cool the liquid. A general view of the cooling tank is shown in Fig. 7. The brine, after cooling, was circulated through the vessel by means of the centrifugal pump shown in Fig. 8. The temperature of the shell of the vessel was measured by means of four thermocouples welded to the heads.

When these thermocouples indicated essential uniformity of temperature at the outside surface, and the temperature of the returning brine was about −50° F, the circulating pump was stopped and the vessel connected to the pressure pump. The pressures were recorded at the instrument console as shown in Fig. 9. Both indicating and recording gages were used.

All spectators were seated in the grandstand at a distance of 200 ft. from the test vessel. Among the spectators were representatives of the ASME Boiler Code Committee, the Pressure Vessel Research Committee, various agencies of the Federal Government, as well as many other interested engineers.

For convenience, the two preliminary burst tests and the two final burst tests will be discussed together, although the vessels were not tested in that order. The final tests two months later were repetitions of the preliminary tests so that duplicate data are available for each test condition.

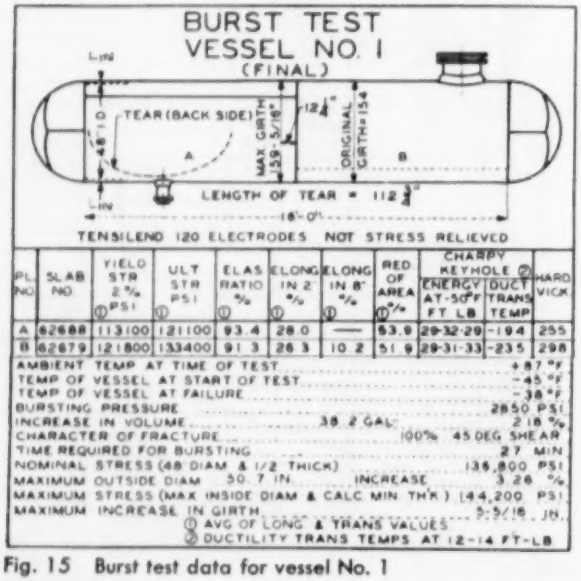
Burst Test of Vessel No. P1 (Preliminary)

The vessel on the burst-test stand as shown in Fig. 10 is down to temperature and covered with frost. The men have removed the circulating piping and are securing the pressure piping. Several girth measuring tapes can be seen. These were held by clamping devices so arranged that the tape slipped as the vessel expanded. The amount of expansion could be read at intervals. The burst is shown in Fig. 11. Failure occurred in the shell at the junction with the side of the nozzle. The shell was trying to expand and the rigid nozzle was trying to prevent it from doing so, concentrating the strain in the welded joint. When that joint reached the limit of its capacity to adjust, failure occurred. The flap shown in Fig. 12 opened, and the reaction threw the vessel off the stand. The fracture was a perfect 45-degree shear with no evidence of cleavage fracture.

The complete data for this test are given in Fig. 13. The maximum girth and the length and location of the tear are shown on the sketch. The mechanical properties of the two shell plates, which in this particular instance happen to be from the same slab, are also given.

It will be noted this vessel failed at an internal pressure of 2740 psi, which resulted in a nominal membrane stress of 131,600 psi. In other words, the joint at the side of the nozzle adjusted and redistributed the strain so well that the vessel was able to take a nominal membrane stress of about 11,000 psi in excess of the strength of the plate as determined by coupon testing. The maximum stress based on the final dimensions of the vessel was 135,000 psi.

The number of strokes of the pump were recorded by a counter at the instrument console. The volume per stroke was known from the dimensions



PL NO	SLAB NO	YIELD STR 2% PSI ①	ULT STR PSI ①	ELAS RATIO % ①	ELONG IN 2" % ①	ELONG IN 8" % ①	RED OF AREA % ①	CHARPY KEYHOLE ② ENERGY AT -50°F FT LB	CHARPY KEYHOLE ② DUCT TRANS TEMP	HARD VICK
A	62688	113100	121100	93.4	28.0	—	53.6	29-32-29	-194	255
B	62679	121800	133400	91.3	26.3	10.2	51.9	29-31-33	-235	298

AMBIENT TEMP AT TIME OF TEST	+87 °F
TEMP OF VESSEL AT START OF TEST	-45 °F
TEMP OF VESSEL AT FAILURE	-38 °F
BURSTING PRESSURE	2850 PSI
INCREASE IN VOLUME 38.2 GAL	2.18 %
CHARACTER OF FRACTURE	100% 45 DEG SHEAR
TIME REQUIRED FOR BURSTING	27 MIN
NOMINAL STRESS (48 DIAM & 1/2 THICK)	135,800 PSI
MAXIMUM OUTSIDE DIAM 50.7 IN. INCREASE	3.26 %
MAXIMUM STRESS (MAX INSIDE DIAM & CALC MIN THK)	144,200 PSI
MAXIMUM INCREASE IN GIRTH	5-5/16 IN.

① AVG OF LONG & TRANS VALUES
② DUCTILITY TRANS TEMPS AT 12-14 FT-LB

Fig. 15 Burst test data for vessel No. 1

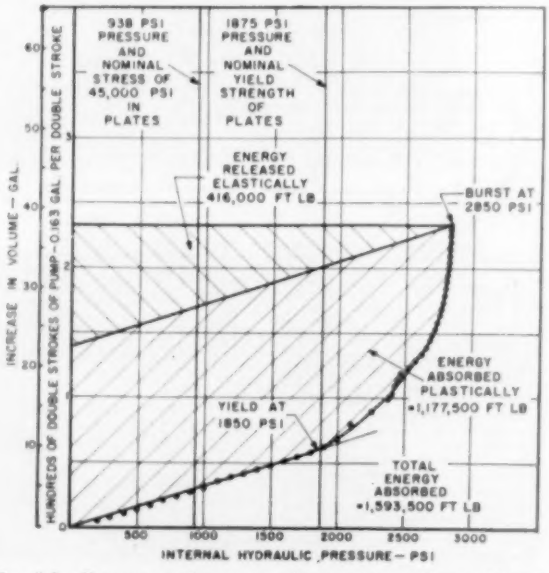

938 PSI PRESSURE AND NOMINAL STRESS OF 45,000 PSI IN PLATES
1875 PSI PRESSURE AND NOMINAL YIELD STRENGTH OF PLATES
ENERGY RELEASED ELASTICALLY 416,000 FT LB
BURST AT 2850 PSI
ENERGY ABSORBED PLASTICALLY =1,177,500 FT LB
YIELD AT 1850 PSI
TOTAL ENERGY ABSORBED =1,593,500 FT LB
INCREASE IN VOLUME — GAL.
HUNDREDS OF DOUBLE STROKES OF PUMP — 0.163 GAL PER DOUBLE STROKE
INTERNAL HYDRAULIC PRESSURE — PSI

Fig. 16 Pressure-volume curve for burst test of vessel No. 1

of the pump. The number of double strokes plotted against pressure is shown in Fig. 14. It will be noted that the vessel behaved elastically up to a pressure of 2000 psi, corresponding to a membrane stress of 96,000 psi. This stress is about 6000 psi greater than the minimum yield strength and about 17,000 psi less than the actual yield strength of the plates as given in Fig. 13. From then on the vessel was expanding plastically. On occasion, dirt prevented the valves of the pressure pump from seating properly; also some leakage at the gaskets occurred so that the volume measurements probably are somewhat in error. However, it is believed that the curve shown depicts fairly well the pressure-volume conditions.

Integrating the total area under the curve will give the total energy absorbed by the vessel.

When failure occurs, it is assumed that the load falls off in a straight line parallel to the elastic pressure-volume curve. Integrating the area of the triangle thus formed will give the elastic-deformation energy which is released when the vessel fails. It will be seen in Fig. 14 that the elastic energy thus released was somewhat less than one-third of the plastic energy absorbed by the vessel. The energy which was released caused the tear in the vessel. If the energy is divided by the length of the tear, it will be found that the energy per inch of tear is about 3370 ft-lb. As will be seen later, this value was essentially constant in all cases in which the base metal was tearing. Had brittle failure occurred, the value of energy per inch of fracture would have been much lower from that shown.

Burst Test of Vessel No. 1 (Final)

The data for the test of vessel No. 1 are shown in Fig. 15. It will be seen that failure in this instance occurred at the small nozzle. It is possible that the stress concentration at the small nozzle was less than that at the large, but it is believed that the failure at the latter is explained by the fact that plate *B* was more than 11,000 psi stronger than plate *A*. The nominal stress at failure was 136,800 psi, about 15,000 lb more than was obtained from coupon tests of the plate. Again the tear exhibited the ductile 45-degree-shear fracture.

The pressure-volume curve is shown in Fig. 16. The similarity between this curve and that shown in Fig. 14 is apparent, but the total energy absorbed by vessel No. 1 was slightly less than that by vessel No. P1. The energy per inch of tear was found to be 3700 ft-lb, slightly more than in the previous case, although the two yield strengths were practically equal.

Results of Burst Test of Vessel No. P3 (Preliminary)

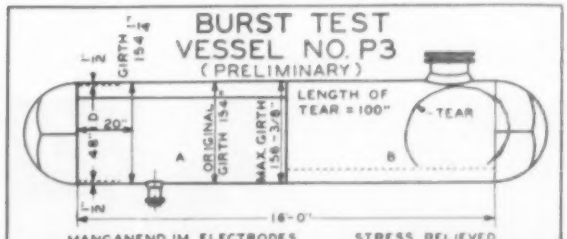
The results of the test on vessel No. P3 are shown in Figs. 17 and 18. This vessel was stress relieved. It has been pointed out that the highly alloyed Ni-Mo-V weld metal necessary to develop 100% joint efficiency is subject to precipitation hardening by stress relieving to such an extent that the toughness of the deposited metal is virtually destroyed. Accordingly, it was necessary to use an E9015 electrode of the manganese-molybdenum type. This electrode deposits weld metal which will not consistently develop 100% joint efficiency in the strong quenched and tempered steel. In vessel No. P3 it will be seen, however, that full joint efficiency was developed and that fracture occurred at the side of a nozzle as in previous cases. The nominal membrane stress was about 5000 psi greater than the strength of the plate as determined by coupon tests.

Trouble was encountered in the functioning of the pump during this test and the data for the elastic portion of the curve are not trustworthy, but the data for the plastic portion are believed to be accurate. It seems reasonable to assume that the elastic portion of the curve can be projected back to the origin with the same slope as those of the other elastic curves. This was done, as shown in Fig. 18. The curve is marked "assumed" but it probably represents the true condition fairly well. Again, the fracture was the perfect 45-degree-shear type. The energy absorbed per inch of tear was found to be about 3530 ft-lb.

Burst Test of Vessel No. 3 (Final)

The burst test data for vessel No. 3 are given in Figs. 19 and 20. The type of failure occurring in this vessel was different from that which obtained in the others. In this vessel, the welded joint in plate *A* broke due to the fact that the weld metal was not quite able to develop 100% joint efficiency. Despite this fact, the membrane stress attained a value of 136,000 psi, about 6000 psi more than the strength of the plate. The energy absorbed, however, was small—as was the increase in girth. The failure was 45-degree shear in the base metal, and was predominantly shear in the weld metal.

The energy absorbed per inch of tear in this vessel was about 2710 ft-lb due to the fact that a large percentage of the length of the tear was in weaker weld metal. It will also be noticed in Fig. 19 that the amount of energy absorbed



PL NO	SLAB NO	YIELD STR 2% PSI ①	ULT STR PSI ①	ELAS RATIO % ①	ELONG IN 2" % ①	ELONG IN 8" % ①	RED OF AREA % ①	CHARPY KEYHOLE ② ENERGY AT -50°F FT LB	CHARPY KEYHOLE ② DUCT TRANS TEMP	HARD VICK
A	62683	124300	130800	94.9	25.8	10.7	49.9	34-30-31	-235	292
B	62677	117700	124000	95.0	27.0	9.7	53.0	32-32-30	-200	283

AMBIENT TEMP AT TIME OF TEST	+71 °F
TEMP OF VESSEL AT START OF TEST	-45 °F
TEMP OF VESSEL AT FAILURE	-37 °F
BURSTING PRESSURE	2690 PSI
INCREASE IN VOLUME ..... 27.2 ..... GAL 1.54	%
CHARACTER OF FRACTURE ..... 100 % 45 DEG SHEAR	
TIME REQUIRED FOR BURSTING	209 MIN
NOMINAL STRESS (48" DIAM & 1/2" THICK)	129,200 PSI
MAXIMUM OUTSIDE DIAM ..... 49.8 IN ..... INCREASE	1.63 %
MAXIMUM STRESS (MAX INSIDE DIAM & CALC MIN THK)	128,000 PSI
MAXIMUM INCREASE IN GIRTH	2-3/8 IN

① AVG OF LONG & TRANS VALUES
② DUCTILITY TRANS TEMPS AT 12-14 FT-LB

Fig. 17 Burst test data for vessel No. P3

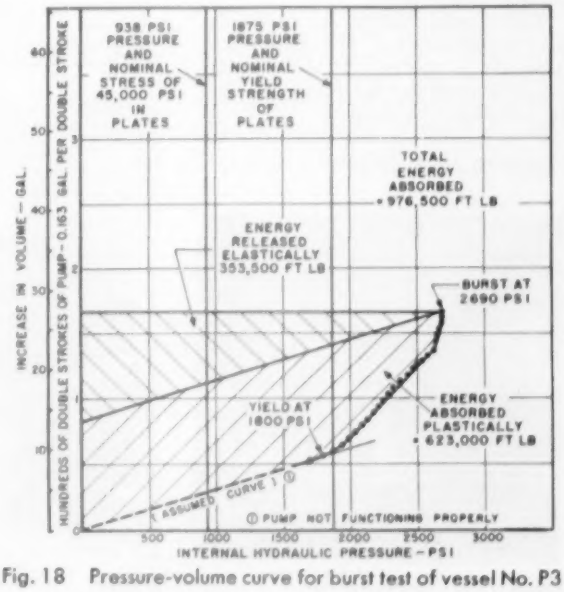
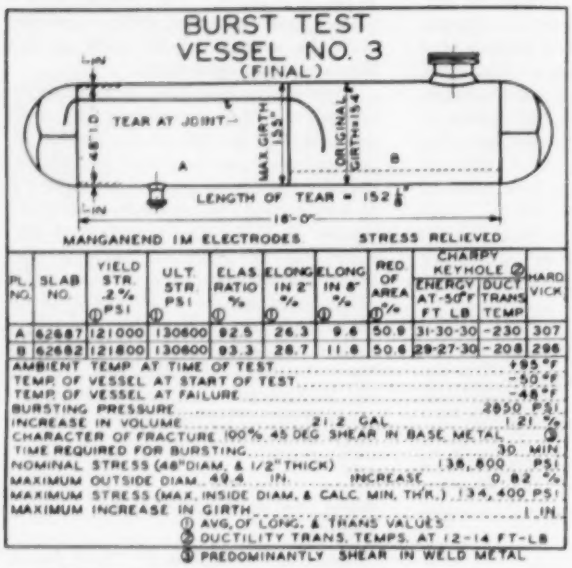

938 PSI PRESSURE AND NOMINAL STRESS OF 45,000 PSI IN PLATES
1875 PSI PRESSURE AND NOMINAL YIELD STRENGTH OF PLATES
TOTAL ENERGY ABSORBED = 976,500 FT LB
ENERGY RELEASED ELASTICALLY 353,500 FT LB
BURST AT 2690 PSI
YIELD AT 1800 PSI
ENERGY ABSORBED PLASTICALLY = 623,000 FT LB
ASSUMED CURVE ①
① PUMP NOT FUNCTIONING PROPERLY
INCREASE IN VOLUME — GAL.
HUNDREDS OF DOUBLE STROKES OF PUMP - 0.163 GAL. PER DOUBLE STROKE
INTERNAL HYDRAULIC PRESSURE — PSI
500
1000
1500
2000
2500
3000

Fig. 18 Pressure-volume curve for burst test of vessel No. P3



PL. NO.	SLAB NO.	YIELD STR. .2% PSI ①	ULT. STR. PSI ①	ELAS. RATIO % ①	ELONG. IN 2" % ①	ELONG. IN 8" % ①	RED. OF AREA % ①	CHARPY KEYHOLE ② ENERGY AT -50°F FT LB	CHARPY KEYHOLE ② DUCT. TRANS. TEMP.	HARD. VICK.
A	62687	121000	130600	92.5	26.3	9.6	50.9	31-30-30	-230	307
B	62682	121800	130600	93.3	28.7	11.6	50.6	29-27-30	-208	296

AMBIENT TEMP AT TIME OF TEST	+95 °F
TEMP. OF VESSEL AT START OF TEST	-50 °F
TEMP. OF VESSEL AT FAILURE	-48 °F
BURSTING PRESSURE	2850 PSI
INCREASE IN VOLUME	21.2 GAL. 1.21 %
CHARACTER OF FRACTURE 100% 45 DEG SHEAR IN BASE METAL	③
TIME REQUIRED FOR BURSTING	30 MIN.
NOMINAL STRESS (48"DIAM. & 1/2"THICK)	136,800 PSI
MAXIMUM OUTSIDE DIAM. 49.4 IN.	INCREASE 0.82 %
MAXIMUM STRESS (MAX. INSIDE DIAM. & CALC. MIN. THK.)	134,400 PSI
MAXIMUM INCREASE IN GIRTH	1 IN.

① AVG. OF LONG. & TRANS. VALUES
② DUCTILITY TRANS. TEMPS. AT 12-14 FT-LB
③ PREDOMINANTLY SHEAR IN WELD METAL

Fig. 19 Burst test data for vessel No. 3

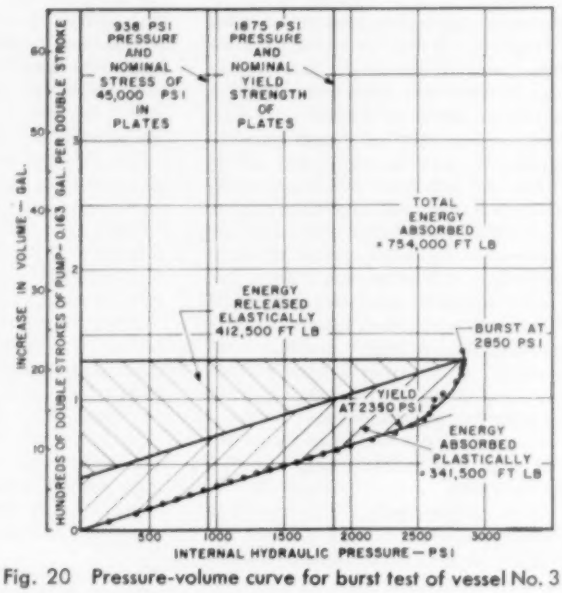


Fig. 20 Pressure-volume curve for burst test of vessel No. 3

plastically was very small, since that weld broke before the plate had elongated very much.

The burst of this vessel, shown in Fig. 21, was most spectacular. A large volume of liquid was thrown toward the grandstand and stopped practically at the feet of the spectators. A detail of the fracture is shown in Fig. 22.

The foregoing data for all the vessels are tabulated in Table 3.

It will be seen that the stress-relieved vessels performed in much the same manner as did the as-welded so far as bursting pressure and membrane stress are concerned. From the standpoints of energy absorbed and increase in volume, however, the as-welded vessels performed better than did their stress-relieved counterparts.

The increase in girth and in volume may appear small at first glance, but it should be pointed out that this increase in girth has not taxed the steel to its capacity to elongate. The material is capable of considerably more elongation than indicated above but was not permitted to expand to its capacity by the failure at the stress concentration.

Again it should be emphasized that these vessels withstood more pressure than they could have been expected to endure, and then failed in a perfectly ductile manner.

Drop-Impact Tests

Considerable thought was expended in determining the best kind of a test to demonstrate the safety of quenched and tempered steel in a pressure vessel. The burst test has been criticized in the case of structural carbon steels on the grounds that when the vessel finally burst, so much ballooning and cold work had

Table 3—Summary Data for Vessels Tested

	As-welded		*Stress relieved*	
	P1	*1*	*P3*	*3*
Hydraulic pressure at failure, psi	2,740	2,850	2,690	2,850
Membrane stress, nominal, psi	131,600	136,800	129,200	136,800
Plastic energy absorbed, ft-lb	1,332,500	1,177,500	623,000	341,500
Elastic energy released, ft-lb	394,000	416,000	353,000	412,500
Energy per inch of tear, ft-lb	3,370	3,700	3,530	2,710
Total energy absorbed, ft-lb	1,726,500	1,593,500	976,500	754,000
Increase in volume, %	2.38	2.18	1.54	1.21
Maximum increase in diameter, %	2.66	3.26	1.63	.82

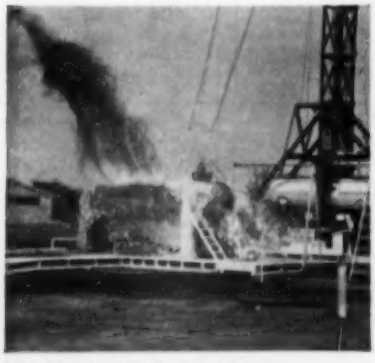


Fig. 21 Burst of vessel No. 3

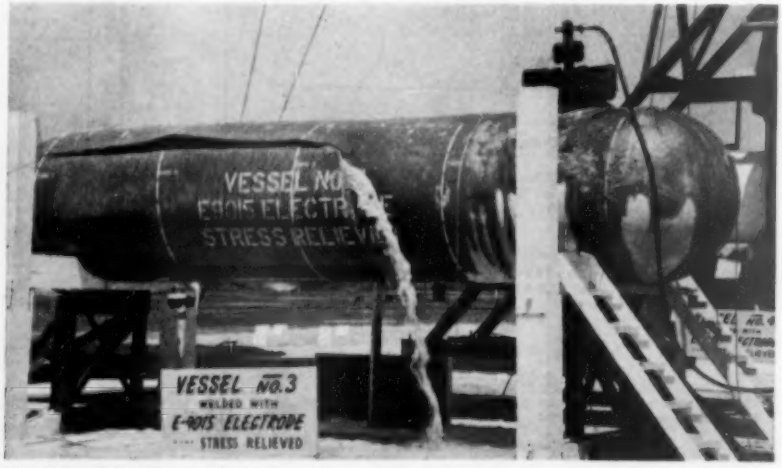


Fig. 22 Failure in welded joint

taken place that the object that finally failed was not the original vessel. This contention is not so pertinent in the case of quenched and tempered materials because, as has been seen, only moderate ballooning takes place in the burst test. Fatigue has been used to some extent as a performance test, but it was not believed that fatigue testing would be suitable because, as was pointed out in a recent résumé by Dolan,[4] and also by Stout,[5] in most instances fatigue is not a real factor in the performance of a pressure vessel. Fatigue strength of weldments is much more sensitive to design than to material and, in addition, no correlation between fatigue strength and toughness has ever been established. Of course if a vessel is really subject to fatigue loading, then fatigue should be given consideration in the design.

Since toughness is one of the most important characteristics of the quenched and tempered steel, it appeared that a test that would show the toughness of the steel, that is, its ability to resist abuse under adverse conditions, would best demonstrate the safety of vessels constructed of this material.

Toughness can be investigated by impact testing, or by bend testing, or by tension testing. Regardless of the method of loading selected large amounts of energy would be needed for the destruction of full-scale vessels. This energy can be gotten in a number of different ways, but obtaining it from the impact of a falling weight seemed most practical.

At least two different methods of carrying out the drop-impact test are possible: (*a*) A series of duplicate pressure vessels could be made, possibly 15 or 20. These could be tested in triplicate for the determination of the transition temperature of this design of vessel over a range of temperatures as is done with small specimens. Aside from the fact that the cost of such a procedure would be prohibitive, such testing could not have been carried out on vessels made of "T-1" steel because lower temperatures than are obtainable by calcium-chloride brine would be needed. Other refrigerants capable of providing lower temperatures would be flammable or altogether out of the question as regards cost. (*b*) On the other hand, individual vessels could be tested at about the minimum atmospheric temperature and their resistance to increasing impact measured. Dropping of a weight from increasing heights was selected as being the most suitable but will not enable one to determine the lowest possible temperature at which the actual vessel will be safe to operate. Such a decision must still be made on the basis of small-scale laboratory experiments.

Velocity is not much of a factor in the drop tests. The maximum velocity obtainable with a net drop of 122 ft is but 88 ft per second. Welded specimens of this material have been subjected to explosion-bulge testing in which the velocity of the pressure wave is many thousands of feet per second; and the performance of the bulge specimens under such ballistic velocities was excellent.

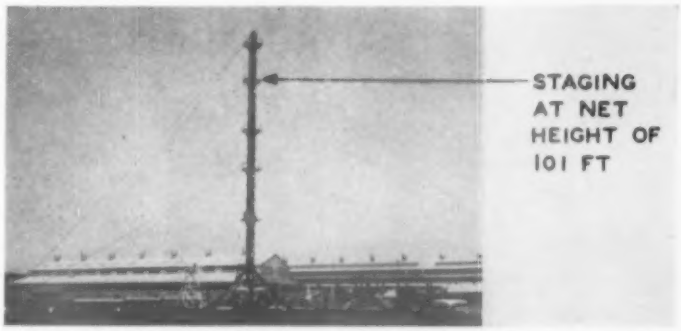


Fig. 23 General view of test tower, total height 149 ft

The question might be asked: what is the relation between the amount of energy absorbed by incremental dropping to that which would be absorbed in a single drop capable of extending the material to its utmost? Considerable incremental drop testing has been done in the past by the writer and it has been found by integrating the areas under the elastic portions of the drop-deflection curve and those under the plastic portions, that the amount of energy absorbed elastically is very small, and that the cumulative total energy of incremental dropping is not far different from that which would have been absorbed if it were possible to select that drop, which would just exhaust the capacity of the material. Another virtue of the incremental dropping is that the succeeding drops fall on material which has been cold worked by the previous impacts and provides information on the performance of the material after having been strained.

In the first preliminary test, the pressure in the vessel was permitted to fall as a result of the increase of the size of the vessel. Each succeeding drop was made with decreased pressure in the vessel. In the later tests this fall in pressure occurred but the vessels were repressurized after each drop. It was found that the amount of pressure in the vessels had no significant effect upon their performance.

Special equipment was designed and fabricated by the Birmingham plant of the Chicago Bridge & Iron Co. to enable this drop testing to be done. A general view of the test tower with stagings at various levels is shown in Fig. 23. The data for the tower are given in Table 4. The vessel rested upon rockers as shown in Fig. 24. A saddle was placed over the vessel to take the direct impact of the tup. The latter, made from an ingot, weighed 26,700 lb.

Upon impact, the frost was shaken from the vessel, as shown in Fig. 25. The ingot rebounded, sometimes as much as 7 or 8 ft. Severe damage was inflicted on the saddle and on the foundations. In the course of the tests, three saddles of various designs were required.

The tup was released by a 20-ton toggle triphook such as is used in the skull crackers in steel mills. While men were working around the equipment, the tup was securely held by means of a safety pin inserted through a yoke.

Table 4—Data for Tower

1. Kinetic data

Net fall, ft	*Kinetic energy, ft-lb*	*Time of fall, sec*	*Velocity at impact, ft/sec*
122	3,260,000	2.75	88.5
101	2,700,000	2.50	80.5
73	1,950,000	2.13	68.5
52	1,390,000	1.80	58.0
24	640,000	1.22	39.2

2. Maximum height of tower ..... 149 ft
3. Capacity (line pull) of winch ..... 4000 lb
4. Distance between rockers ..... 14 ft
5. Total weight of structural steel:
   - In tower and rocker support ..... 24.5 tons
   - In striking saddle ..... 5900 lb

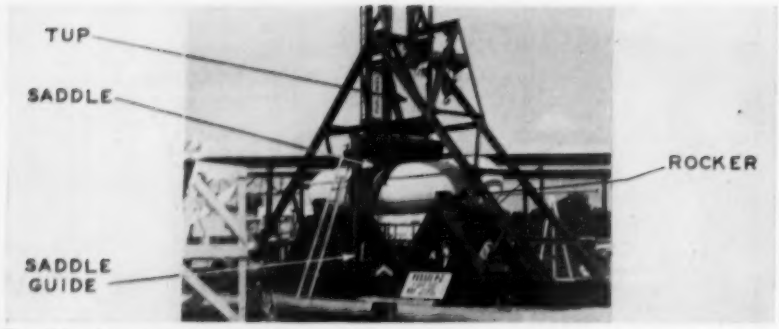


Fig. 24 Arrangements at bottom of tower

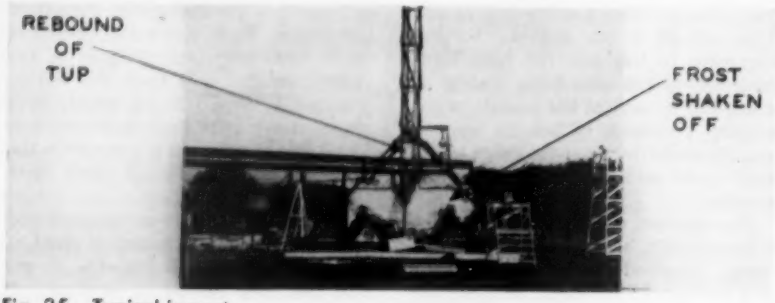


Fig. 25 Typical impact

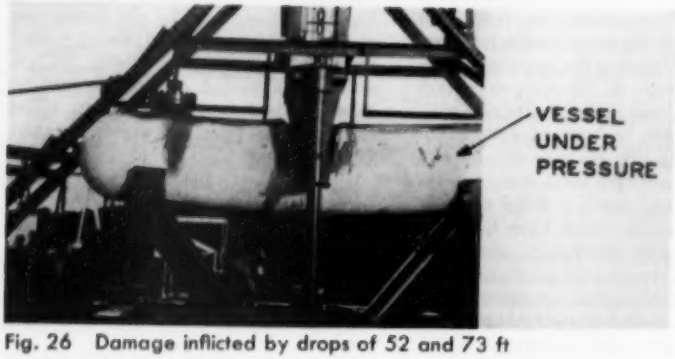


Fig. 26 Damage inflicted by drops of 52 and 73 ft

Table 5—Drop-Impact Test Data for Vessel No. P2 (Preliminary), Welded with E12015 Electrodes, Not Stress Relieved

	Drop No. 1	*Drop No. 2*	*Drop No. 3*
Internal pressure after previous drop	...	1070 psi	780 psi
Vertical sag after straightening	...	Not measured	Not measured
Vertical indentation after straightening	...	Not measured	Not measured
Ambient temp. at time of test	64° F	62° F	60° F
Temp. of vessel at time of drop	−39° F	−34° F	−31° F
Pressure in vessel at time of drop	1560 psi	1070 psi	780 psi
Net fall of tup	52 ft	73 ft	101 ft
Energy of impact	1,390,000 ft-lb	1,950,000 ft-lb	2,700,000 ft-lb
Velocity of impact	58 ft/sec	68.5 ft/sec	80.5 ft/sec
Maximum hydraulic pressure at impact	Not measured	Not measured	Not measured
Character of fracture	None	None	45° shear
Vertical sag	Not measured	Not measured	Failure
Vertical indentation	1⅜ in.	2 15/16 in.	Failure
Nominal stress (48 in. diam and ½ in. thick)	74,900 psi	51,300 psi	37,400 psi
Maximum outside diam	50.1 in.	50.8 in.	51.5 in.
Outside diam increase	2.04%	3.46%	4.89%
Maximum stress (max inside diam calc min thk and impact pressure)	...	...	...

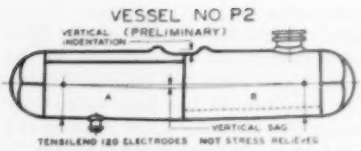


Fig. 27 Measurement of vertical sag and indentation

This safety pin was withdrawn 10 seconds before the tup was dropped.

In one preliminary test, darkness set in before the dropping was completed. In the darkness, it was possible to see sparks generated by the friction of the tup falling in the guides. Flame was also seen when the ingot struck the saddle.

As the drop testing progressed, indentation was forced into the vessel, as can be seen in Fig. 26 and a small amount of bending occurred. While it was fully understood from general principles of physics that thin-walled vessels, subjected to internal hydraulic pressure, would behave in a manner similar to that of a solid piece of steel, it was, nevertheless, surprising to observe this thin-walled vessel resist bending so stoutly.

The pressure at impact was measured by means of a Pflader pressure cell mounted on the foundation and connected by a flexible hydraulic hose to the vessel. The bonded wire strain gage in this instrument transmitted impulses to a Brush recorder at the console.

After a drop was made, the pressure was raised to 2000 psi and then permitted to drop to 1875 psi. This was done to assure the safety of the men working around the vessel. The application of pressure to a bent tube causes a straightening effect such as obtains in a Bourdon tube, and under the 2000-psi pressure the vessels did straighten almost to their original shape. The vertical sag and vertical indentation were measured as shown in Fig. 27.

The data for the performance of the four vessels are given in Tables 5 to 8, inclusive. Inasmuch as the performance of all vessels was very similar, the results of all can be discussed together. It will be seen that all four vessels endured cumulative drops up to 73 ft, and that all failed as a result of the 101-ft drop.

A typical failure in the drop test is shown in Fig. 28. The vessels broke completely apart as can be seen in Fig. 29, and failure started at the bottom near the girth seam, since this was the point of maximum bending moment. Failure in all cases, both in the burst tests and in the drop tests, started near the toe of the weld and moved inward, and progressed at a 45-degree angle away from the weld, as shown in the insert sketch, Fig. 29.

Once the circulation of the cooling brine had been stopped and the vessels

sealed off, a gradual warming took place under the hot sun and the rise in temperature of the vessels as the testing proceeded will be noted. Whether the pressure in the vessel was allowed to fall as in the case of vessel No. P2, or whether the vessel was repressurized as in the case of vessels Nos. 2, P4 and 4, made no difference in the performance. It will be noted that the vertical indentations in the case of vessel No. P2 were considerably greater than those obtaining in the subsequent vessels which were straightened after each drop. In the latter, the indented noncylindrical section was forced out into a shape more closely approaching a cylinder.

The increase in diameter as a result of the drop testing was remarkably constant. The transient pressures at impact ranged from 50 to 90% greater than the static pressures, and the transient stresses reached values as high as 187,000 psi. It is interesting to note that these vessels endured high transient stresses twice before failure. Vessel No. P2 was given a preliminary drop of 24 ft to try out the testing arrangements. Accordingly, the data for drop No. 1 are, in reality, the data for a second drop. It will be noted that this preliminary test caused the pressure in the vessel to fall from 1875 psi to 1560.

Again it will be seen that performances of the stress-relieved vessels were very similar to those of the as-welded. Certainly no benefit could be attributed to stress relief.

Significance

As was stated earlier, the overall conclusion as to whether or not "T-1" steel is suitable for the construction of pressure vessels will be decided by the ASME Boiler Code and other agencies governing the design and construction of pressure vessels. In this paper no conclusions, as such, will be drawn, but the significance of the various aspects of the tests will be pointed out in detail.

1. Strength

The four vessels tested by bursting exhibited static strengths considerably in excess of tensile values found by coupon testing of the plates. Three of the four vessels tested by drop impact were subjected to static membrane stresses equal to the published yield strength of the material and, in addition, twice endured transient stresses ranging from 30 to 80% greater than the published ultimate strength of the plates. All of these strength values were obtained with the steel at a low temperature and in the presence of concentrations of stress. It would seem, therefore, that from the standpoint of strength, the quenched and tempered plates were capable of giving an excellent account of themselves in pressure vessels.

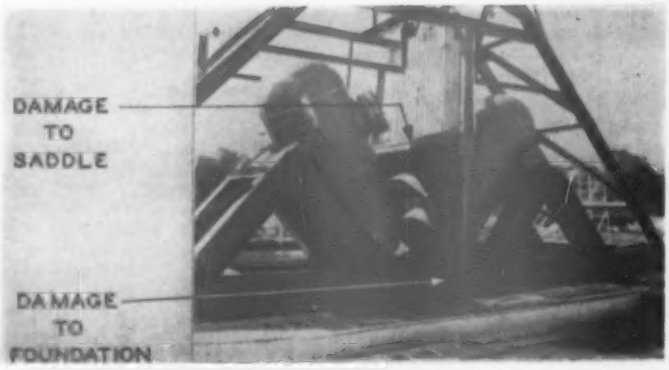


Fig. 28 Typical failure by drop of 101 ft

Table 6—Drop-Impact Test Data for Vessel No. 2 (Final), Welded with E12015 Electrodes, Not Stress Relieved

	Drop No. 1	*Drop No. 2*	*Drop No. 3*
Internal pressure after previous drop	...	1230 psi	1090 psi
Vertical sag after straightening	...	Not measured	Not measured
Vertical indentation after straightening	...	Not measured	Not measured
Ambient temp. at time of test	94° F	98° F	99° F
Temp. of vessel at time of drop	−33° F	−26° F	−22° F
Pressure in vessel at time of drop	1875 psi	1860 psi	1875 psi
Net fall of tup	52 ft	73 ft	101 ft
Energy of impact	1,390,000 ft-lb	1,950,000 ft-lb	2,700,000 ft-lb
Velocity of impact	58 ft/sec	68.5 ft/sec	80.5 ft/sec
Maximum hydraulic pressure at impact	2800 psi	3200 psi	3300 psi
Character of fracture	None	None	45° shear
Vertical sag	9/16 in.	2 3/8 in.	Failure
Vertical indentation	3/4 in.	1/2 in.	Failure
Nominal stress (48 in. diam and 1/2 in. thick)	90,000 psi	89,400 psi	90,000 psi
Maximum outside diam	50.2 in.	51.5 in.	51.6 in.
Outside diam increase	2.45%	3.06%	5.30%
Maximum stress (max inside diam calc min thk and impact pressure)	139,000 psi	159,000 psi	173,000 psi

Table 7—Drop-Impact Test Data for Vessel No. P4 (Preliminary), Welded with E9015 Electrodes, Stress Relieved

	Drop No. 1	*Drop No. 2*	*Drop No. 3*
Internal pressure after previous drop	...	0*	1030 psi
Vertical sag after straightening	...	3/16 in.	1 13/16 in.
Vertical indentation after straightening	...	0 in.	1/8 in.
Ambient temp. at time of test	90° F	90° F	93° F
Temp. of vessel at time of drop	−34° F	−28° F	−22° F
Pressure in vessel at time of drop	1880 psi	1880 psi	1820 psi
Net fall of tup	52 ft	73 ft	101 ft
Energy of impact	1,390,000 ft-lb	1,950,000 ft-lb	2,700,000 ft-lb
Velocity of impact	58 ft/sec	68.5 ft/sec	80.5 ft/sec
Maximum hydraulic pressure at impact	2910 psi	Not measured	Not measured
Character of fracture	None	None	45° shear
Vertical sag	5/8 in.	2 11/16 in.	Failure
Vertical indentation	1 3/8 in.	1/4 in.	Failure
Nominal stress (48 in. diam and 1/2 in. thick)	90,300 psi	90,300 psi	87,400 psi
Maximum outside diam	50.2 in.	50.9 in.	51.6 in.
Outside diam increase	2.24%	3.88%	5.30%
Maximum stress (max inside diam calc min thk and impact pressure)	147,000 psi	...	...

* Break in pressure cell connection.

Table 8—Drop-Impact Test Data for Vessel No. 4 (Final), Welded with E9015 Electrodes, Stress Relieved

	Drop No. 1	Drop No. 2	Drop No. 3
Internal pressure after previous drop	...	1320 psi	1060 psi
Vertical sag after straightening	...	1 1/4 in.	3 1/4 in.
Vertical indentation after straightening	...	1/2 in.	0 in.
Ambient temp. at time of test	100° F	100° F	102° F
Temp. of vessel at time of drop	−41° F	−35° F	−30° F
Pressure in vessel at time of drop	1880 psi	1860 psi	1890 psi
Net fall of tup	52 ft	73 ft	101 ft
Energy of impact	1,390,000 ft-lb	1,950,000 ft-lb	2,700,000 ft-lb
Velocity of impact	58 ft/sec	68.5 ft/sec	80.5 ft/sec
Maximum hydraulic pressure at impact	Not measured	3200 psi	3500 psi
Character of fracture	None	None	*
Vertical sag	1 1/4 in.	3 1/16 in.	Failure
Vertical indentation	1 in.	2/4 in.	Failure
Nominal stress (48 in. diam and 1/2 in. thick)	90,300 psi	89,300 psi	90,700 psi
Maximum outside diam	50.5 in.	51.3 in.	51.7 in.
Outside diam increase	2.85%	4.48%	5.30%
Maximum stress (max inside diam calc min thk and impact pressure)	...	167,000 psi	187,000 psi

* 45° shear in base metal, predominantly shear in weld metal.

2. Ductility

The usual shop operations of rolling, pressing, flanging and dishing were performed satisfactorily, showing that the material possesses adequate ductility to enable pressure vessels to be fabricated.

The amount of ballooning exhibited by the vessels in the burst test was smaller than that which would have been expected from the elongations obtained from streamlined tension specimens.

The amount of indentation endured by the vessels in the drop tests was more than any engineering structure could be expected to endure, and failure, when it finally occurred, came at the point of maximum bending moment at the bottom and not at the point of impact where damage was becoming increasingly severe, and not at the nozzles where concentrations of stress obtained.

Since neither the burst test nor the drop test exhausted the capacity of the steel to elongate, and since both types of tests subjected the vessels to abuse more severe than could ever be permitted in service, it would seem that the ductility of the quenched and tempered plate was adequate for pressure vessel operation.

3. Toughness

In all cases failure was characterized by 45-degree shear fractures indicating that ductile failure was occurring at the lowest temperatures involved in the testing, that is, the material possessed sufficient ductility under adverse conditions of stress concentration and low temperature to permit the minute adjustments necessary to bring about ductile failure. These failures all occurred at extremely high stress levels. Despite the fact that the ductility-transition temperatures obtained from Charpy impact tests of fabricated plate are very low, it is believed advisable to limit the application of "T-1" quenched and tempered constructional alloy steel to vessels operating at temperatures no lower than the lowest obtaining in atmosphere anywhere on the globe. This conservative limitation takes into consideration the impairment to toughness brought about by welding and other shop fabrication operations.

Fig. 29 Ultimate damage to vessel

4. Stress Relieving

There was no significant difference between the performance of the vessels tested in the as-welded condition even though they contained residual stresses on the order of 80,000 psi, and the vessels tested after having been stress relieved confirming the belief that if a material is tough at the operating temperature, stress relieving—or more properly, restoration of toughness—is not necessary.

5. Uniformity

Plates from 14 slabs from the same heat were used in the construction of these vessels. The remarkable uniformity of mechanical properties as determined by coupon testing and as demonstrated in tests of the full-scale vessels demonstrates the uniformity possible with quenched and tempered plate.

6. Yield-Tensile Ratio

The yield-tensile ratios of the plates used in the construction of these vessels were extremely high, in some cases as high as 97%. The remarkable performance of these vessels in "Operation T-1" demonstrates that a high yield-to-tensile ratio is satisfactory.

7. Working Stress

The strength exhibited by the four vessels tested by bursting, and the capacity of the four vessels tested by impact to endure extremely high transient stresses, are considered ample evidence that vessels made of this quenched and tempered plate would be capable of operating at very high working stresses, much higher than have ever been used heretofore.

8. Fatigue

Fatigue data for "T-1" steel in the elastic range and in the plastic range are presented in References 1, 5 and 6. Any discussion of the suitability of a steel for pressure vessel construction cannot ignore fatigue. In using such fatigue data, the first datum which must be established is the anticipated number of cycles of stress which will occur throughout the life of the vessel and the stress level. If the expected number of cycles is low, then it does not seem logical to penalize the vessel by imposing on it fatigue restrictions which are not applicable.

If it is decided that fatigue really is a factor in the operation of the vessel, then it would seem logical to attack fatigue at its most vulnerable point, namely, the local peak stresses. Instead of imposing on all the material in the vessel limitations which are necessary only in small localized areas, it would seem better to refine the design in these areas, and thereby enable greater membrane stresses to be used.

Supplementary Test

The vessels as originally designed contained forged, streamlined nozzles in-

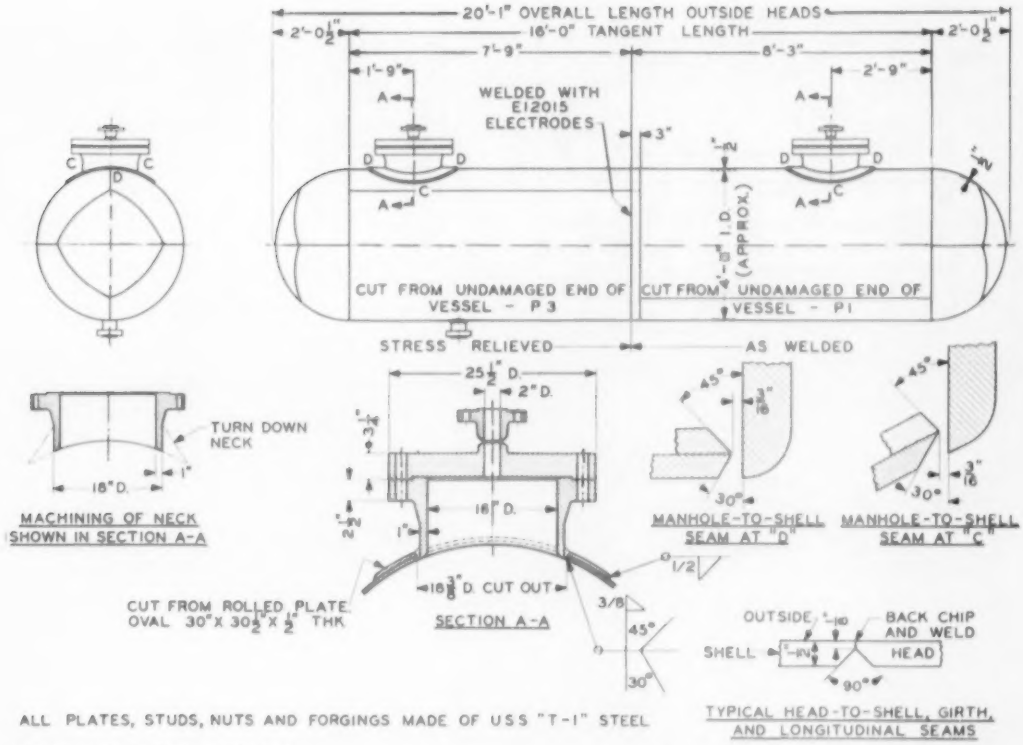


Fig. 30 Design of vessel No. 5 for supplementary burst test

serted into the shell and butt welded as shown in Fig. 6. This type of construction was used to minimize the concentrations of stress at the nozzles as much as possible. Since the Boiler Code permits the use of built-up fillet-welded nozzles, it was decided that an additional test should be conducted to ascertain the performance of such nozzles in a vessel made of this alloy constructional steel.

One half of vessel No. P3 and a half of vessel No. P1 were harmed but little by the original burst testing, and these two halves were welded together to make a ninth vessel which was designated as vessel No. 5. Since in this supplementary test nozzles were of particular concern, it was decided to have the same type of nozzle on each half and to stress relieve one half (before joining) and not stress relieve the other. The weld between the two halves was made with an E12015 electrode.

Two of the original large forged nozzles were turned down and inserted into the vessel and doublers and then complete penetration welds were made. The details of the design are shown in Fig. 30. The test would thus determine the merits of the built-up nozzle versus the forged nozzle, and the relative performance of a stress-relieved built-up nozzle versus an unstress-relieved one.

The test was conducted at the Birmingham plant of the Chicago Bridge & Iron Co. on April 27, 1955. The same equipment and testing technique as was used previously was employed. The results of the test are shown in Fig. 31. The vessel failed in the welded joint in exactly the same manner as did vessel No. 3. The hydraulic pressure was about 80 psi greater and the membrane stress was about 3800 psi more. The increase in volume was less.

The pressure-volume relationship is shown in Fig. 32. It will be seen that the vessel behaved elastically up to a hydraulic stress of 2575 psi, nearly 800 psi more than during the previous test. This corresponds to a membrane stress of 123,500. If the curve shown in Fig. 32 is superimposed on the former curve in Fig. 18, it will be noted that vessel No. 5 yielded at about the intersection of the straight line in Fig. 32 with the plastic portion of the curve in Fig. 18. In other words, the cold working in the previous test had raised the yield point of the steel in accordance with the relationship shown in Fig. 18.

Very little energy was absorbed in plastic deformation, but fracture, when it occurred, was still in the characteristic 45-degree-shear mode. The cold work had not impaired the toughness to a degree sufficient to cause brittle fracture under the temperature and notch conditions prevailing. This performance was most interesting, in that the halves of the original vessels had been severely strained and had then been permitted to age for nearly a year, and yet gave no evidence of brittle failure.

Prior to testing, all four drain plugs into the volume between the doublers and the shell plate were removed. During the testing no leakage occurred at those points, showing that no failure at the inside weld had occurred. Neither the as-welded built-up nozzle nor the stress-relieved nozzle showed any signs of distress. After the test was completed, the nozzles were removed from the vessels and magnetic-particle tested inside and out. No failure was found in the base metal, and only one slight transverse crack was found in the weld of the as-welded nozzle. It is probable that the nozzles would have endured considerably more load had not the weak link, namely, the stress-relieved weld, failed. So far as static tension is concerned, therefore, it would appear that the built-up nozzles were amply able to endure high working stresses in a vessel built of "T-1" steel.

Again, the relative performance of the as-welded half versus that of the stress-relieved half definitely showed no superiority for the latter. In fact, it would appear very doubtful that the stress relieving really accomplished much, inasmuch as the yield strength of the stress-relieved half was apparently diminished very little, if at all, by this thermal treatment.

It is believed that the supplementary test of vessel No. 5 is an interesting con-

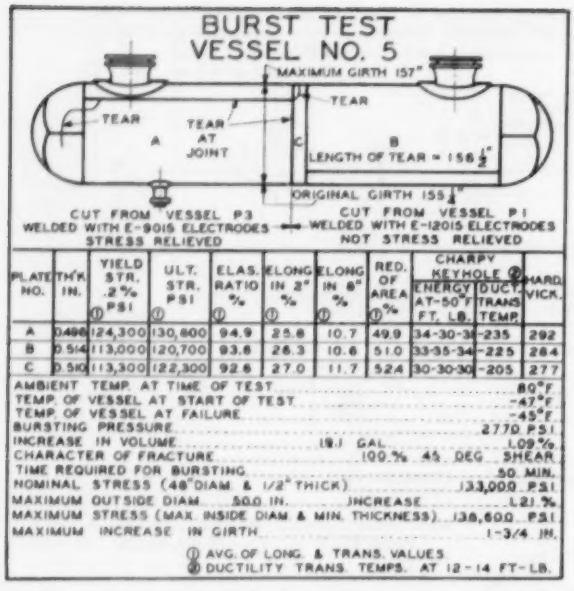


PLATE NO.	THK. IN.	YIELD STR. .2% PSI ①	ULT. STR. PSI ①	ELAS. RATIO % ①	ELONG IN 2" % ①	ELONG IN 8" % ①	RED. OF AREA % ①	CHARPY KEYHOLE ② ENERGY AT -50°F FT. LB.	CHARPY KEYHOLE ② DUCT. TRANS TEMP.	HARD. VICK.
A	0.498	124,300	130,600	94.9	25.8	10.7	49.9	34-30-36	-235	292
B	0.514	113,000	120,700	93.6	26.3	10.6	51.0	33-35-34	-225	264
C	0.510	113,300	122,300	92.6	27.0	11.7	52.4	30-30-30	-205	277

AMBIENT TEMP. AT TIME OF TEST	80°F
TEMP. OF VESSEL AT START OF TEST	-47°F
TEMP. OF VESSEL AT FAILURE	-45°F
BURSTING PRESSURE	2770 PSI
INCREASE IN VOLUME	19.1 GAL. 1.09%
CHARACTER OF FRACTURE	100% 45 DEG. SHEAR
TIME REQUIRED FOR BURSTING	50 MIN.
NOMINAL STRESS (40" DIAM. & 1/2" THICK)	133,000 PSI
MAXIMUM OUTSIDE DIAM. 50.0 IN. INCREASE	1.2%
MAXIMUM STRESS (MAX. INSIDE DIAM & MIN. THICKNESS)	138,600 PSI
MAXIMUM INCREASE IN GIRTH	1-3/4 IN.

① AVG. OF LONG. & TRANS. VALUES
② DUCTILITY TRANS. TEMPS. AT 12-14 FT-LB.

Fig. 31 Burst-test data for Vessel No. 5

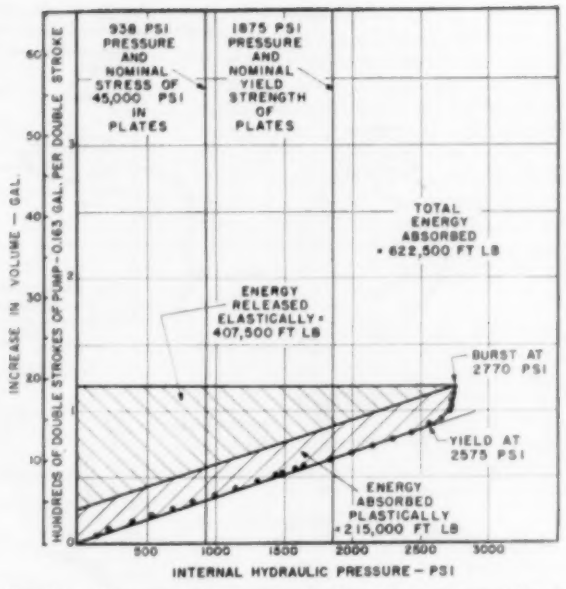

938 PSI PRESSURE AND NOMINAL STRESS OF 45,000 PSI IN PLATES
1875 PSI PRESSURE AND NOMINAL YIELD STRENGTH OF PLATES
TOTAL ENERGY ABSORBED = 622,500 FT LB
ENERGY RELEASED ELASTICALLY = 407,500 FT LB
BURST AT 2770 PSI
YIELD AT 2575 PSI
ENERGY ABSORBED PLASTICALLY = 215,000 FT LB
INCREASE IN VOLUME — GAL.
HUNDREDS OF DOUBLE STROKES OF PUMP - 0.163 GAL. PER DOUBLE STROKE
INTERNAL HYDRAULIC PRESSURE — PSI

Fig. 32 Pressure-volume curve for burst test of vessel No. 5

tribution to our knowledge of the behavior of pressure vessels, in that components of full-scale vessels were twice tested to destruction.

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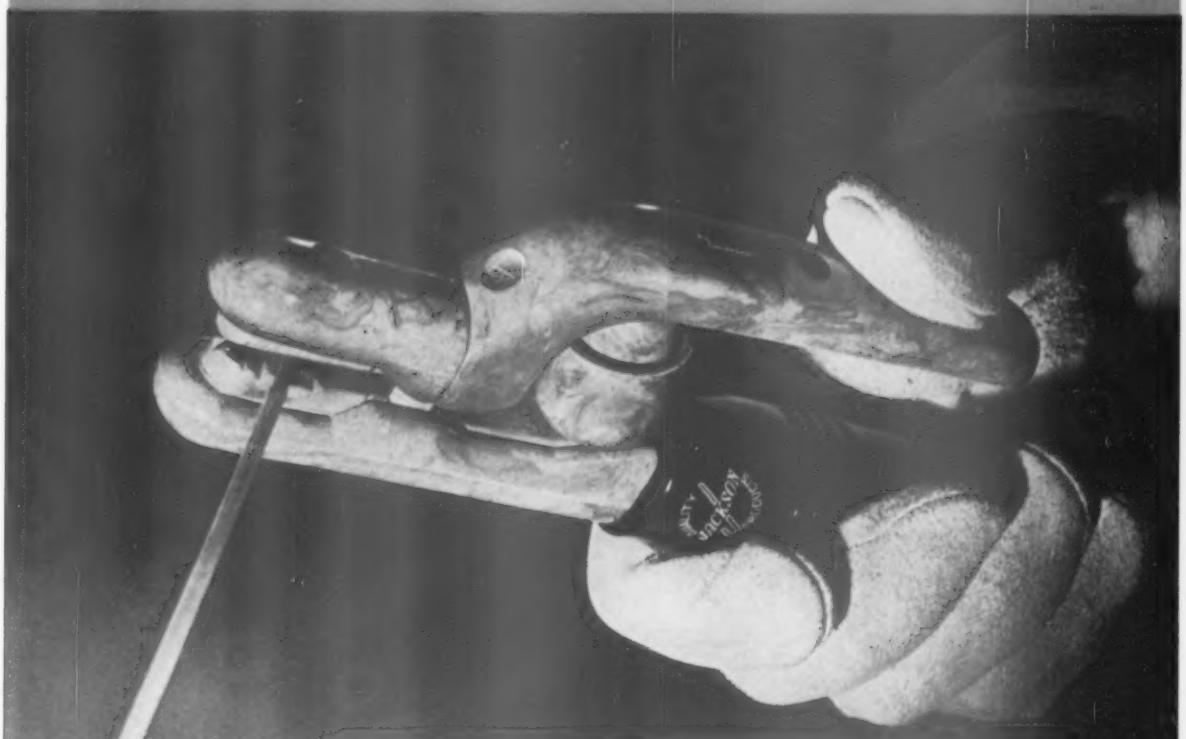

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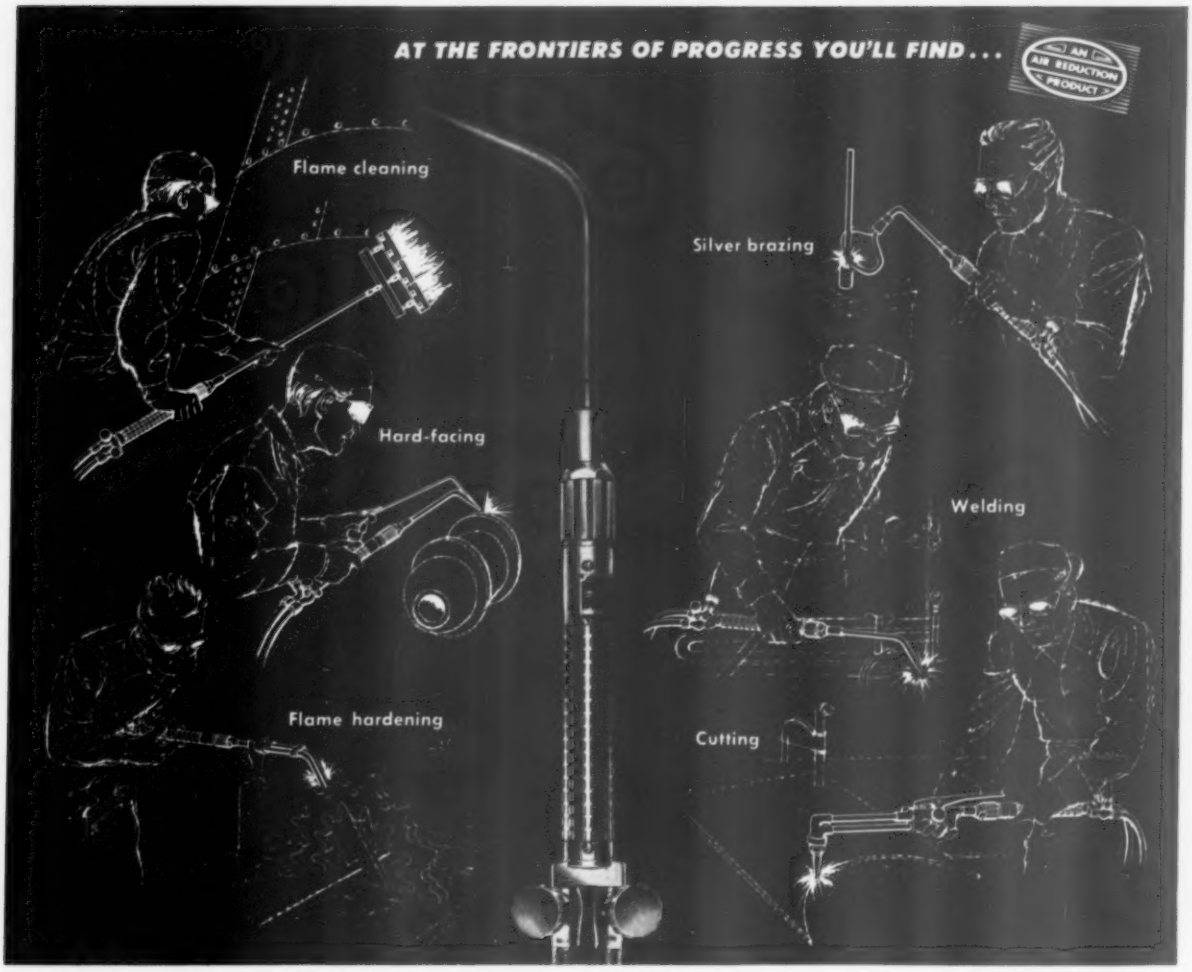

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